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The Journal of the American Welding Society

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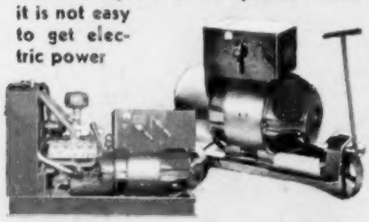

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The American Welding Society Journal

W. SPRARAGEN, Editor

Published for the Advancement of the Science and Art of Welding

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SEPTEMBER 1938

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Amphibian built of stainless steel "Shot-weld" construction. Courtesy, Fleetwings, Inc.

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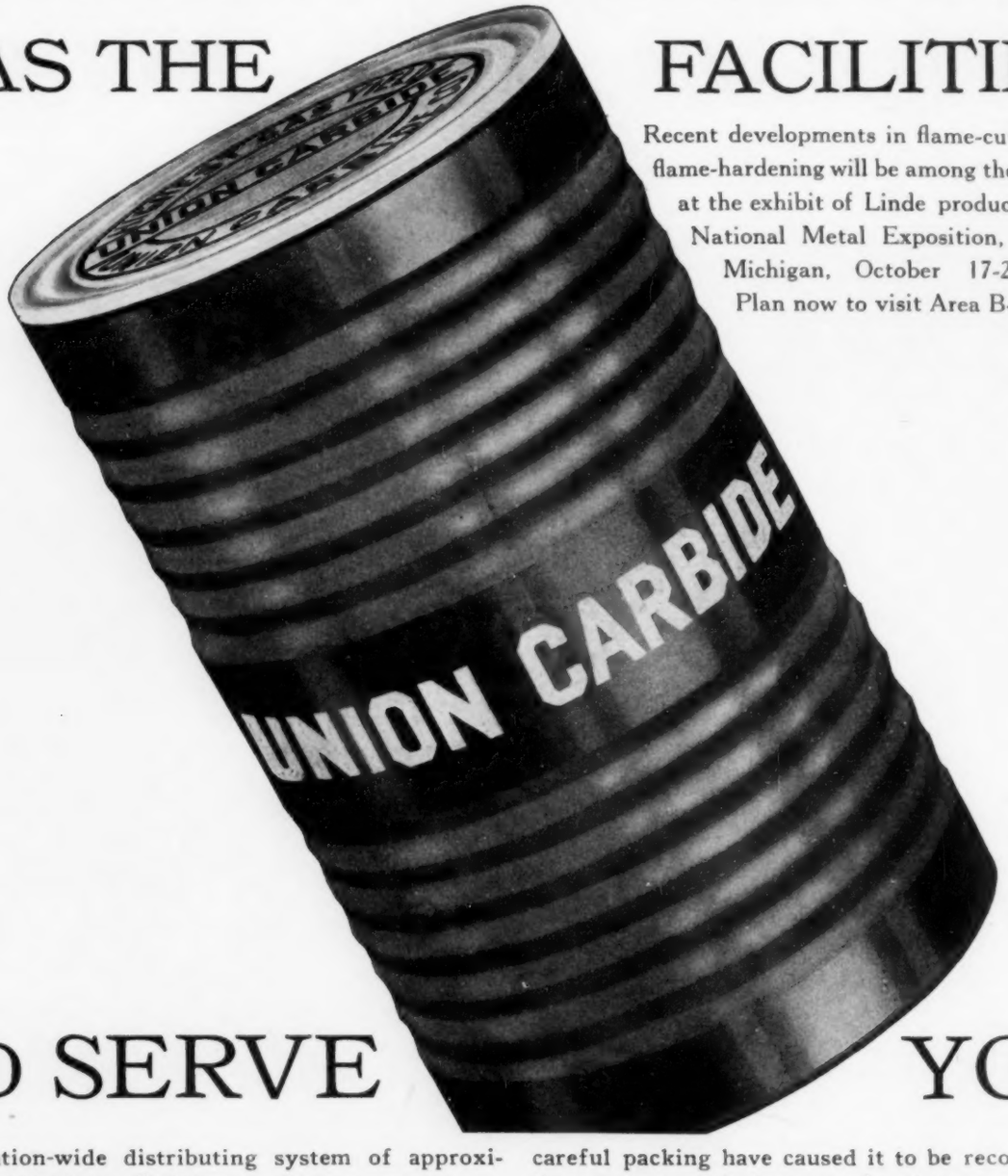

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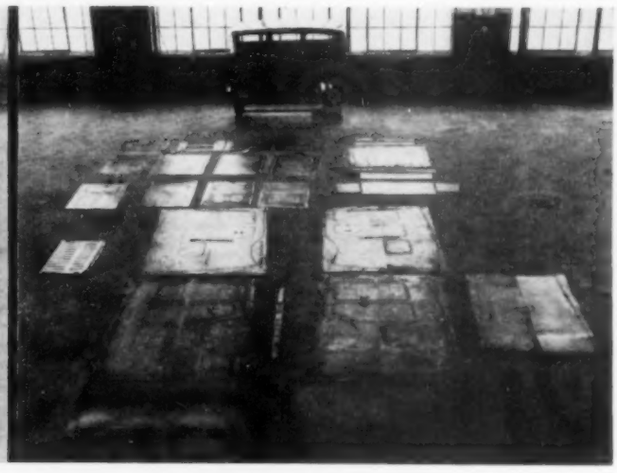
VOLUME 17, No. 9 SEPTEMBER 1938

Built-Up Type Sedan Body—2/21/37



Original Mono-Piece Body—2/21/27

BRIEF HISTORY OF THE BUDD Allsteel Body

By JOSEPH W. MEADOWCROFT†

EARLY in the 1900's, the public was convinced that the automobile was not just a makeshift mechanism, but that it was soon to take its place with other permanent means of transportation. The power unit, which was one of the outstanding problems confronting the automotive pioneers, needed improvements to such an extent that it could be started and stopped at the command of the operator, and it was to the solution of this problem that most of the engineering effort was devoted, while very little thought was given to the construction of the automobile.

THE REAL START

It was not until 1908 that the automobile developed to a controllable vehicle and it then followed that its other parts were in need of improvement in order to insure safety and comfort in this new means of transportation. Competition also promoted general improvements in the automobile and various manufacturers began to specialize in producing such parts as their shops were best adopted. Therefore, the manufacture of automobile parts became distributed over a wide range instead of all or nearly all being produced by one manufacturer.

† Assistant Works Manager, Edward G. Budd Manufacturing Co., Philadelphia, Pa.

THE BUGGY STAGE

The automobile, having developed from the horse-drawn carriage, naturally carried a body that was identical to the body of this carriage. This body soon proved inadequate, being too fragile to withstand the vibration of the improved power unit, and not large enough to accommodate the loads it could now convey, and to meet these new demands a larger body of more sturdy construction was created. This improved body was merely a wooden frame, bolted or screwed together in the same manner as any wooden frame, with a covering to enhance its appearance and furnish a smooth outside surface. Plywood was the first material used for the covering, but, as body contours developed, sheet steel and aluminum were later used to better advantage.

At the Madison Square Garden (New York) Automobile Show, held in January 1910, of approximately sixty automobiles about ninety per cent were of the open or touring car type, while the other ten per cent consisted of limousines and roadsters. The majority of these touring cars had no provision for inclement weather and windshields were extremely rare. Not one of these cars possessed any semblance of a front door, as rear doors were the standard of the time. The body contours being plain, flat metal sheets were bent around the wooden frame and nailed or screwed in place and only in the very

expensive automobiles were the body shells pressed to form.

CONTOURS APPEAR

In 1911 body contours began to appear and plain lines were replaced by curves to improve the appearance of the automobile. This improvement made it necessary to use forming dies and presses along with sheet metal that possessed the proper drawing qualities. At this time front doors also were beginning to appear. These were made by employing the same principle as the body—metal over a wooden frame.

The most difficult task at this time was the painting of the body, which like the painting of a carriage body was a very laborious and costly operation. In some instances the painting operations took as long as twenty-one days, in order to produce the desired finish.

In the manufacturing of a body the fenders and running boards were not included and in some cases were not manufactured by the body builders. The methods for painting fenders and running boards were considerably faster than those used for the body, i.e., dipping in enamel and baked under high temperatures. This method could not have been used with the wooden frame body, for the heat would have been detrimental to the life of the wooden frame.

THE ALLSTEEL BODY

During the year 1909, Mr. Edward G. Budd, who was familiar with body painting problems, began to strive toward a solution to this problem and decided that if no wood were used in the body structure, it would be possible also to dip and bake the body similar to the method employed for fenders and running boards. In June 1912, Mr. Budd made the move which gave his idea tangible form, when he founded the Edward G. Budd Manufacturing Company for the building of the ALLSTEEL automobile body. In that year Mr. Joseph Ledwinka was employed by Mr. Budd to carry on the engineering work.

On June 17, 1914, Mr. Ledwinka applied for the first patent upon the ALLSTEEL automobile body. The object, as stated in the patent, was "to provide an automobile body which is strong and durable, simple in construction and economical to manufacture." Patent No. 1,143,635 was granted to Mr. Ledwinka, and assigned to the Edward G. Budd Manufacturing Company, on June 22, 1915. From the founding of the company in June 1912, until June 22, 1915, when the first patent was granted, the work of building the ALLSTEEL body had progressed without the protection of a single patent.

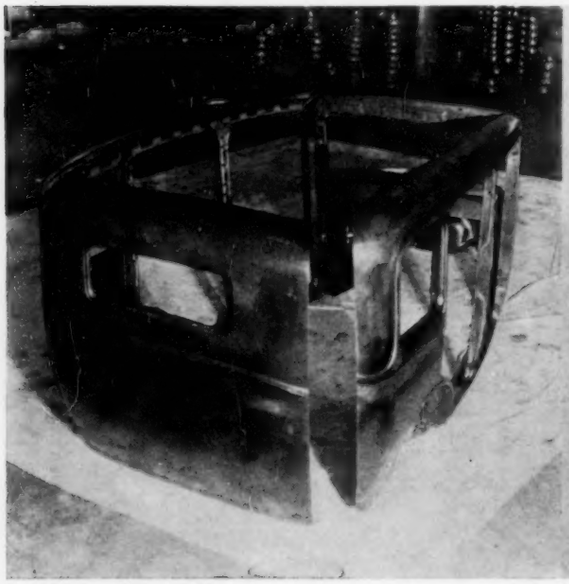
The first Budd ALLSTEEL body consisted of four main sub-units, namely, the outside shell, the inside frame, the seat supports and the doors.

WELDING APPEARS

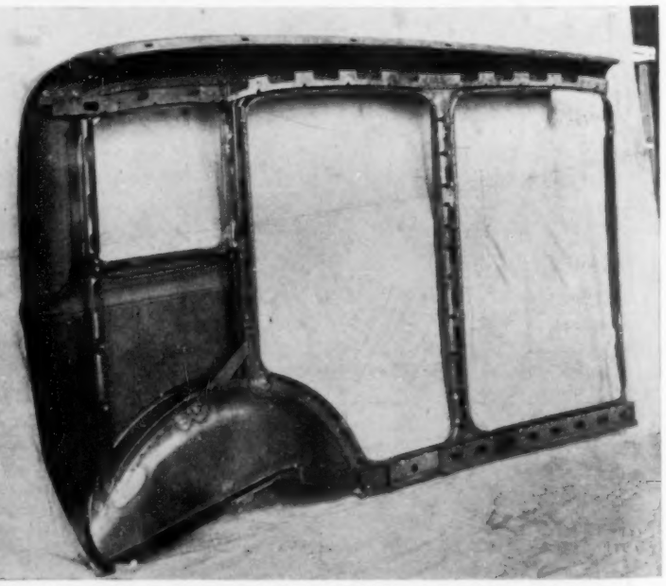
The outside shell was divided into four sections—"C" the front end or "Cowl," "B" the body sides and "A" the tonneau back. These sections were formed from 0.037 inch thick sheet steel and joined together into one unit by the use of spot welding and oxyacetylene welding. The entire periphery of the outside shell was flanged to facilitate assembly with the inside frame.

The inside frame, which supplanted the wooden frame, was constructed along entirely different lines, since it furnished strength and sturdiness, while the outside shell only contributed to the appearance of the body. The inside frame sides were formed as a single piece with edges flanged to correspond with those of the outside shell. The same material was used in the frame as in the shell except that this material was 0.062 inch and 0.093 inch thick. Where possible the sides were blanked and flanged to decrease the weight and increase the rigidity. However, if insufficient metal prevented flanging, depressions were made in the metal to produce a sturdier construction.

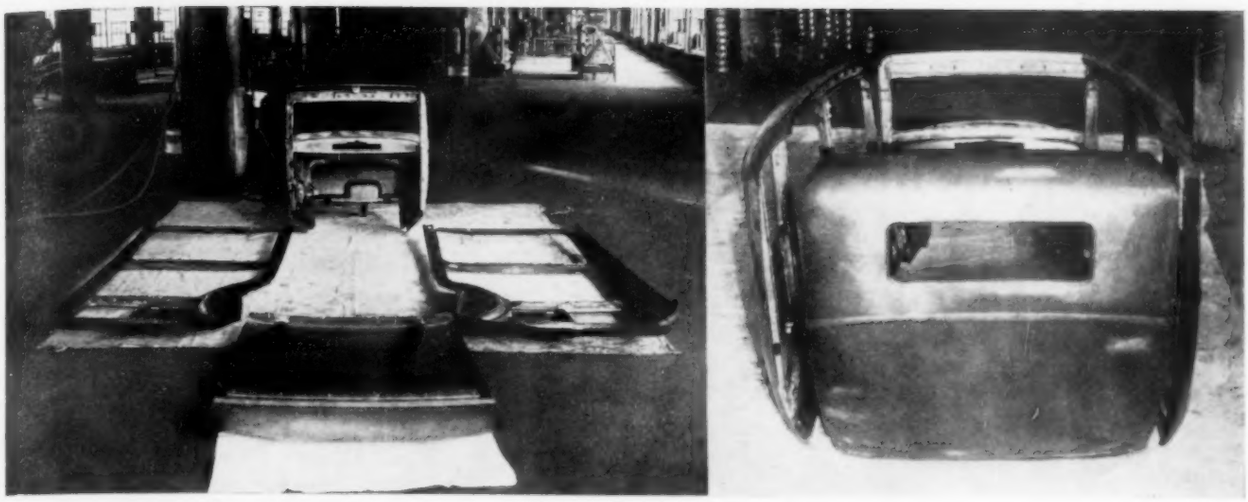
The front of the inside frame was constructed to accommodate the toeboard supports, so that they could be welded in place before the two frame sides were assembled. The frame structure was made intact by welding and riveting two cross-braces, the cowl-pan and the front and rear seat supports to the sides. Practically all these parts were stamped from 0.050-inch sheet metal,



Mono-Piece Body Showing Main Units 11/29/29



Mono-Piece Side Panel 11/29/29



Mono-Piece Body Showing Main Units 11/29/29

or slightly thicker. The cross-braces were pressed into a channel section to improve stiffness. Several impressions were made in the cowl pan for the same reason, although the flange was added to fasten to the outside shell. The pan was riveted to the frame at the toeboard supports.

The seat supports were composed of several parts which were welded together before the support was assembled with the frame sides. Care was taken to make a channel or angle sections in all the necessary parts, so that the assembled supports would amplify the solidity of the frame assembly. The front seat support was quite complex because it was the portion of the assembled body to hold the central part of the frame in proper alignment. The rear seat support was of simpler form since two reinforcement braces were placed on the frame in the extreme rear area to resist external forces.

ASSEMBLY OF PARTS

The final assembly operations were in all probability the most toilsome of all. The first step involved the fitting of the shell over the frame, then joining the two together by welding and riveting. Both units were cumbersome to handle, especially where spot welding was essential. Even when body-building was in its infancy, the welding operations overlapped the riveting work, despite the fact that the spot-welding machine was less flexible than the riveting tool. In order to adapt the welding machines to this new work, some were elevated on wooden blocks to secure the correct clearance for some operations and other machines were strapped horizontally to the concrete columns of the building several feet above the floor line. Where it was impossible to use a welding machine, and riveting was undesirable or impractical, oxyacetylene welding was employed. The doorway flanges of the outside shell and the inside frame were spot welded together with the welds located about three inches apart. To perform this particular task the body had to be moved through all conceivable angles in order to place the welds at the most effective points. The welding of the body-bottom flanges together was a relatively elementary operation, since it consisted of continuous welding along a nearly straight line. A reinforced channel section was thus maintained at the lower part of the doorways. The frame lower rear brace was welded to the bottom flange of the shell tonneau in this same operation. The front seat panel was finally riveted at the bottom to the front seat support, and oxyacetylene welded to the top of the body sides between the doorways.

The door followed the same design as that of the body. The outside panel provided the finished surface, while the inside panel furnished the rigidity and stiffness required of this sub-unit. Several small pieces and reinforcement strips were welded to the inside panel to strengthen it, as were the hinges and the lock also. After this preliminary work, the inside panel was fitted to the outside panel and the edges of the outside panel were clinched over those of the inside panel. The assembled doors were fastened to the body unit by pushing the hinge leaves into openings in the body provided for that purpose, and bolting the hinges in place.

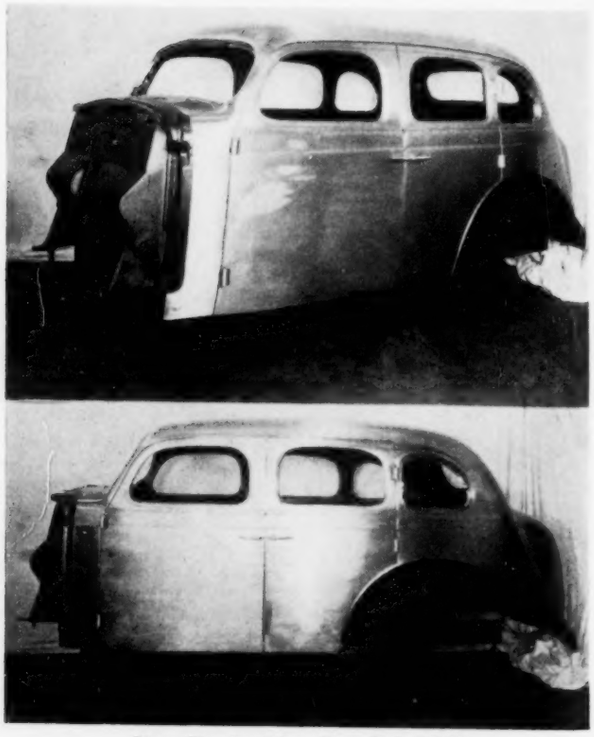
The finishing touches were added to the body before it underwent final inspection. Oxyacetylene welding was the main tool in this step. Any doubtful joints were permanently sealed and rough spots were made smooth and polished over to meet with inspection approval. The finished product was then covered with a coating of oil to prevent rusting during transit.

The completed body bore little resemblance to its kin on the street. Floors of any type did not exist. In comparison to the bulky composite body with a wooden frame, this new creation appeared to be a mere egg-shell but, on the contrary, it was much stronger and roomier than the composite body. No valuable passenger space was absorbed by thick wooden frame members. All its parts and sections were made from thin sheet metal which occupied far less space and of less weight than wood used to meet the same demands. Furthermore, the body could be dipped in paint and baked without injury of any nature and at a vast saving of time.

CONSTANT CHANGES

The Budd ALLSTEEL automobile body as originally designed did not long survive. From its inception, changes and improvements were constantly made and within four years the body design had undergone considerable alteration. The outside shell was slightly modified while the inside frame had received the bulk of the changes.

One major change was an increase in body width which necessitated a deeper impression in the rear tonneau



Views Showing Mono-Piece Body 5/6/36

stamping to allow clearance for the rear wheels. It was found difficult to incorporate this feature to the tonneau as the metal invariably fractured in the wheel-house area. Thus, the tonneau stamping was blanked and flanged in that region so that a stamped filler piece could be welded in place to provide the required clearance. Another change involved the addition of a reinforcing rail which was welded around the upper contour flange of the tonneau to promote sturdiness.

THE CONVEYOR

During the year 1918 a long assembly conveyor running parallel with the floor was installed to speed up the final operations of the body. These included hammering, straightening, polishing, filing, gas-welding at various points, door hanging and final inspection.

From 1918 to 1922 the automobile body again changed. This time the frame structure experienced little variation in design except that the side sills were made wider and stronger.

The outside shell, being of a less rigid structure, was very difficult to handle and thus it was decided that if smaller units could be made they could be handled much easier and with a corresponding saving in time. The outcome of this was that the shroud was built as a single unit. The space between the doorways was reduced in width and became a part of the side sills. The wheel-house now occupied a larger arc on the tonneau sides, and the tonneau itself was a separate unit. The doors no longer possessed round corners at the bottom. A summary of these changes indicate that the body units at this period were: (1) Shroud, (2) Body Side Sills, (3) Rear Tonneau, (4) Seats and (5) Doors.

THE CLOSED BODY APPEARS

During 1921 a new type of roof was invented to convert touring cars into closed body cars. This device was known as the "California Top." In summer it could be removed and replaced by the collapsible top. The demand for this attachment was so great that automobile manufacturers were prompted to consider seriously the building of a permanently closed body.

The Edward G. Budd Manufacturing Company played a vital and conspicuous part in the development of a moderately priced closed body, due to the fact that the ALLSTEEL construction was so readily adapted to the closed body design. In 1923 the closed bodies made their appearance on the street. Instead of using four small doors, two larger ones served the purpose. Among the points of note regarding the closed body was the absence of a front seat support. The collapsible front seats, demanded by the two-door style, could not be made as an integral part of the new body.

No top of any kind was provided, simply a superstructure was added to the touring car body so that a top could be permanently located thereon by the automobile manufacturer.

JIGS

Final assembly jigs and fixtures had begun to show their usefulness in this type of work. Some jigs had been used on the touring car body, but with the advent of the closed body, large assembly jigs were indispensable. Their function was to maintain proper relation of the body units until they were either welded or riveted in place. The body was then removed from this "oversize gage" for final welding and riveting. In this manner uniformity was assured for mass production.

By 1927 automobile body designs brought the body much lower. This development was based upon the theory of greater stability and driving ease because of a lower center of gravity. The ALLSTEEL method again proved its flexibility by conforming with these new requirements, and a new phase of ALLSTEEL body design was realized. Thus the "Monopiece" type of body came to the fore as the most radical change since the invention of the ALLSTEEL body.

THE MONOPIECE BODY

The Monopiece body was a complete departure from the square box-like appearance of the closed body. Round contours replaced the square edges and corners wherever possible. Later, automotive engineers, after a protracted struggle with the problems of wind resistance, concluded that flat surfaces were detrimental to the movement of the automobile and they proceeded to minimize this characteristic by sloping these surfaces. Thus "streamlining" first became an actuality.

Just as the name implies, the new Monopiece body was made up of fewer parts. These were necessarily of larger size than the old style and more awkward to handle. To obviate this hindrance, the overhead track system played an important part and the size of the body unit no longer puzzled the shop methods of manipulation.

The Monopiece body was made up of four main units:

1. Front Section
2. Body Side Panels
3. Rear Panel
4. Doors

For some unknown reason the shop name for the shroud was changed to "Front Section." While this unit appeared practically the same, its design underwent considerable change. It will also be noted that by this time four-door sedans had become quite popular.

The new design of ALLSTEEL body completely revolutionized its former design. Originally the inside frame

furnished the structural sturdiness, while the outside shell provided the appearance only. Now, the outside shell could be formed to include such a variety of sections that it constituted the main source of rigidity, and the inside frame played only a secondary part in this respect. The outside shell was made of 0.037 inch thick sheet metal while the inside frame pieces were made from 0.050-inch stock. In reality, there was no inside frame because the side sills had become obsolete and the assembled body was attached directly to the chassis of the automobile.

The front section was made up of the outside shell and the inside panels, with the former being the predominant part of this unit. The shell was first flash-welded together of three stampings and then returned to the press shop for further forming. Then it was sent to the assembly floor where the inside panels, dash pan, toeboard supports and instrument board were welded thereto. As the inside frame was no longer a stiffening unit, the parts which were used in its stead were a series of thin, perforated strips known as inside panels. One of their functions was to maintain the rigidity of the outside shell, and the other was to hold the upholstery.

Each body side panel was blanked and formed in the press shop from a single sheet of metal. The wheelhouse filler piece, which was formerly welded to the outside shell, now became a part of the side panel. The rounded contours at the top and rear were also formed with it. All the doorway corners were rounded and tapered to eliminate door rattles while the automobile was in motion. Channel, angle and other modified sections were pressed throughout the entire stamping to create a sturdy unit.

The rear panel was pressed from the same material as the front section and the side panels. Its design and construction were less complex, and the assembly work required was very small. Most of the operations consisted of straightening, hammering and polishing before it entered final assembly.

The doors were rounded and tapered at the corners to conform with the new scheme applied to the side panels. The inside door frame was no longer a welded unit, but was stamped in one piece to accommodate the window opening.

In the final body assembly, the flash-welding method proved its value. The right and left-hand side panels and the rear body panel were provided with special flanges for this operation. The rear panel was placed into the flash-welding machine with the exterior surface downward and clamped in position. The side panels were then set in place so that the flange of each panel would coincide with the corresponding one on the rear panel.

STREAMLINING

Shortly after the Monopiece body was introduced, streamlining took the automobile industry by storm. The changes in the ALLSTEEL body, from the year 1931 have been almost continuous, each successive change approaching nearer to the design offering a minimum of wind resistance.

The actual body design, however, was beyond the jurisdiction of the body builder, his responsibility being only to construct the exact reproduction of the automobile manufacturer's approved model. The only concern to the automobile manufacturer was the weight, exact dimensions and contours of this body, while compactness and sturdiness of the body lay within the body-builder's judgment as to the method of approach.

These streamlined styles taxed the fabricating phase of the ALLSTEEL body building industry considerably more than the engineering phase. The front section of this new design was partially absorbed by the side panels and was no longer made as a distinct unit. This new streamlined Monopiece body consisted of: (1) The Side Panels, (2) Rear Panel, (3) Peak Panel, (4) Cowl Panel, (5) Dash Pan and (6) Doors.

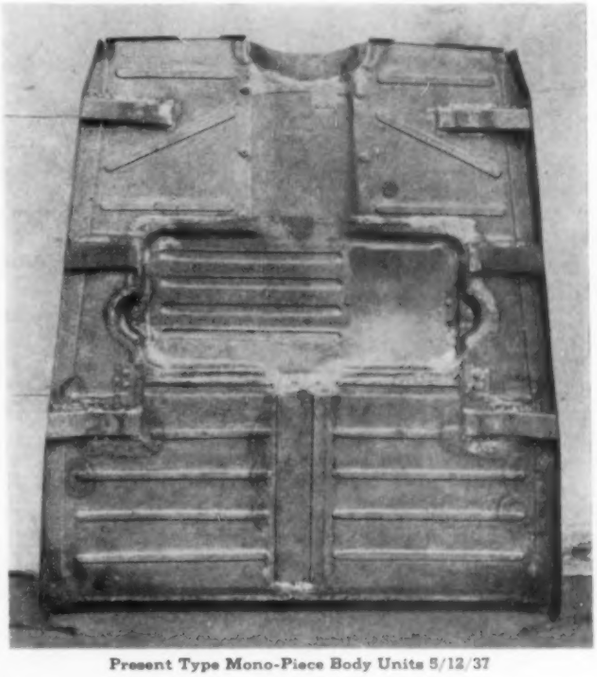
The side panel stamping was blanked and formed from a single sheet of steel. With the sweeping curve at the rear and the added front section area, the forming of this single unit was an achievement which reflected credit upon the minds responsible for its creation. With the creation of this design side panel shop practice also underwent extensive changes.

SPOT WELDING

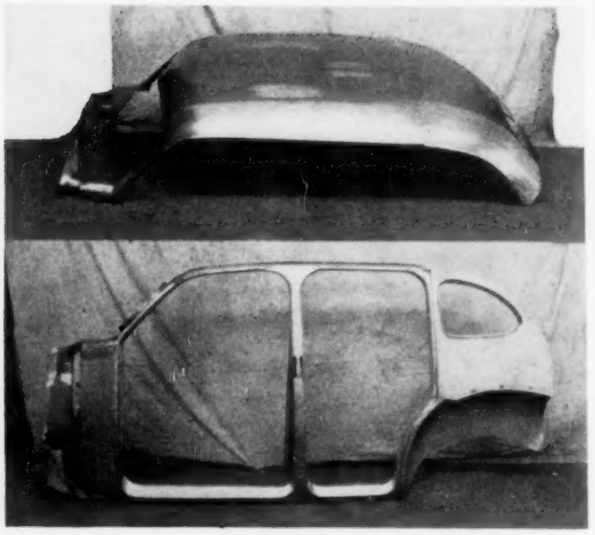
Spot welding was the only assembly method used, as riveting proved impractical in this particular unit. Since the upright welding machines could not cope with the new welding conditions, portable spot-welding devices, known as "Pinch-welders," were adopted. These hand-operated machines or "guns" were fitted with flexible copper cables for carrying the electric power to the electrodes fastened to them. Thus, the operator could apply this tool to any part of the side panel.

The rear, peak and cowl panels involved little shop work. The rear panel operations were slightly changed from the previous ones. The peak and cowl panels, being new arrivals, required stiffening elements before being incorporated with the final assembly. The door designs and assembly work remained unaltered.

The final body assembly required two flash-welding machines. The first machine was identical to that used on the Monopiece body to unite the rear panel and the two side panels. The second machine flash-welded simultaneously the peak and cowl panels to the front end of the side panels. These operations were in continuous order, that is, the united panels were removed from the first machine and placed into the second without any intervening shop work.



Present Type Mono-Piece Body Units 5/12/37



Views Showing Present Mono-Piece Body Units

Up to this juncture all the bodies had been delivered to the automobile manufacturer as a roofless ALLSTEEL unit. The side panels, rear and peak panels terminated shortly after the roof contour was completed, thereby leaving a large open area at the top of the body. To fill this space, a wooden frame, covered with a special waterproof cloth was constructed. As each body emerged from the enamel-baking oven and cooled, this cover was added, then a gummy compound was poured around the entire edge of the cover to fill any possible crevices and produce a waterproof shelter.

This form of a wooden frame cover was discarded in favor of a reinforced sheet-steel roof panel, which is still being used by some automobile manufacturers. The edges of the roof opening were formed into a Z-section to accommodate the steel roof and to improve the structural rigidity of that body area. The roof panel was so constructed that the edges could be bolted in position, and its addition to the ALLSTEEL body provided bracing advantages which were impossible to obtain with the old style wooden roof.

ALLSTEEL ROOF

From the detachable steel roof panel came the idea of a permanent steel roof as an integral part of the body. This would also create a homogeneous upper body structure. Another noteworthy feature adopted was the steel floor which in some cases extended from the rear seat pan to vertical column sections connecting the upper and lower parts of the doorways. Also, the sill sections at the lower part of the doorways were formed into more complex cross sections to promote even greater stiffness than in the previous designs. This brings us to the present practice in body design and assembly work.

Most current four-door sedans contain a trunk in the rear part, built in as a part of the actual body. The rear panel arrangement was thus disrupted, and the trunk-lid fills the greater portion of that space. The front part of the body has also undergone some important changes in design. The cowl is no longer a flat sheet with impressions added as a reinforcing medium.

The embodiment of the features just mentioned brought almost complete renewal in the design of the body units and method of assembly. The body is now composed of: (1) Roof Panel, (2) Side Panels, (3) Dash Pan, (4) Trunk Lid and (5) Doors. It is seen that the rear panel, peak and cowl panels are not used in this design.

PRESENT-DAY BODIES

The forming of the new steel roof-panel from a single sheet of steel constituted almost as great a triumph as that which accompanied the creation of the Monopiece body side panel. As can be seen from the photograph, it occupies a greater portion of body surface than any other unit. It includes the rear window area down to the point where the trunk opening begins. The entire upper and rear curved contours of the side panels also form an integral part of the roof panel. At the front, the peak and cowl panels and part of Post "A" are included in this unit.

The body side panels are mere skeletons of their old designs. Each panel terminates at the upper doorway openings, where they match with the roof panel edges. Toward the rear they sweep back to the trunk opening, filling in the space below the roof panel and the bottom line of the body. The wheel-housings are still pressed as a part of the side panel. The sills at the lower doorways are more complex in cross section than previously and so shaped as to give a flat supporting surface for the new type of floor.

The parts and inside panel pieces entering this new side panel have not varied in design to any appreciable degree. The same general procedure is used to apply and weld the component stiffening parts into the panel unit. The real problem surrounding this unit is the final assembly involving its union with the roof panel. This will be taken up later.

The dash pan is no longer a single flat piece of thick sheet metal. Its construction is of such a nature that several pieces of 0.062-inch metal are welded and riveted together to comprise what is known as the Dash Pan Assembly. The actual design is radically changed from the old style.

One additional part of this style of body is the trunk-lid. Its construction is identical to that of the doors with the exception of a more curved design. It is hinged in place by a heavy metal strip welded to the roof panel and braced by a metal strap at each end near the hinges. These straps are welded to the rear window flange and to the rear seat pan thereby providing a support for these at back.

The door design is not sufficiently changed to warrant extensive detail. Probably the greatest change in the assembly procedure is the automatic conveyor for welding the parts to the inside panel. The upright welding machines have been outmoded in favor of the new type overhead pneumatic welding units. These operate at such speed that production has been greatly accelerated.

The final assembly is the most difficult task of all because such large units are involved, and no flash-welding operations exist. The dash-pan assembly is welded to the roof panel before they enter the final assembly jig. The side panels are also brought into the jig as well as the rear seat pan, and floor. The edges of the roof panel were made to overlap those of the side panels at all points of contact so that the three units could be spot-welded together. All the units are tacked together in the jig for rewelding and finishing when the body assembly is removed therefrom.

The ordinary final operations characteristic of the final assembly conveyor produced the finished body at the unloading end. After the customary oil spraying operation, the body is ready for delivery.

THE SIMPLE STEEL ANALYSIS and Its Relation to Quality in Steel

By R. W. SANDELIN†

INTRODUCTION

IN THIS present day of highly specialized uses for steel, the definition of "quality," when referring to steels in general, takes on a rather broad aspect, and it might safely be said here and now that the relative merits of a particular steel are rather incompletely defined by data obtained in a few simple routine tests performed in the laboratory. Performance under actual conditions in service is, of course, the criterion on which one type of steel is determined to be of a better or poorer quality than some other class of steel.

In the present discussion we shall confine our remarks to the plain carbon steels made in the basic open-hearth furnace, and some of the factors which influence their behavior.

In the ordinary routine inspection of steel, it is customary to obtain a few samples of the product for the performance of certain physical tests such as yield and ultimate tensile strengths, ductility as indicated by per cent elongation and per cent reduction in area, hardness, and less frequently the impact values. To these data are added the chemist's report which gives the percentages by weight of some four or five elements and include carbon, manganese, sulphur, phosphorus and silicon. Since, as is commonly known, the physical properties are greatly affected by the amount and nature of cold working, hot working or heat treatment, it quite frequently happens that the chemical analysis data are used as a final basis for comparing the relative quality of the different steels under consideration.

We shall endeavor to point out some of the pitfalls which await the casual observer who attempts to completely define the quality of a steel on the basis of its chemical analysis alone, *because duplication of analysis does not insure duplication of quality.* We know that at the present time, at least, the full interpretation of a steel analysis is not possible although considerable progress toward that end has been made in recent years and to which reference will be made later. However, the ultimate analysis giving the percentages of carbon, manganese, sulphur, phosphorus and silicon does give us a fairly good idea of the type or grade of steel, and the general usages for which such a steel may be assigned by virtue of their generally known capacity for cold or hot work, or heat treatment. Broadly speaking, we know the general effects of the various commonly determined elements on the physical properties of steels, and in that sense the chemical analysis is indeed quite useful. However, in making specific comparisons between steels of the same grade or type but from different sources, discrepancies quite often arise which are unexplainable on the basis of the ultimate analysis figures alone. In these instances of inconsistency of relative quality, it can be said that such "eccentricities" are the result of the presence of less known and very infrequently determined constituents which are usually present in small amounts and determined only with considerable difficulty, but which, like seasoning in food, have far-reaching effects. Every maker and user of steels knows that some steels wholly unsuited for certain purposes may have essentially the same ultimate analysis as some other steel which is highly satisfactory. Quite a number of instances could be cited where some steel of good quality had an inferior analysis while the analysis of a poorer quality steel looked much better—on paper, at least. For example, high phosphorous steels when properly made in the Open-Hearth Furnace have good ductility and strength and may be much superior to other steels having considerably less phosphorus, the other elements of the ultimate analysis being substantially the same. This is quite apart from the average text-book conception of the embrittling effect of phosphorus. In this instance the significant fact to consider is that in one case the steel is rephosphorized and in the other, the phosphorus was residual resulting from a high phosphorous charge and for some reason or other was incompletely worked out. In some cases this may have been due to a rapid drop in carbon content of the steel so that the heat was tapped early in order to finish on the correct carbon analysis. Needless to say, such practice produces very cheap but inferior quality steel.

Contrary also to the popularly conceived notion that sulphur is the cause of "red shortness" and therefore low sulphur steels are always of higher quality than higher sulphur steels, we have observed cases of extreme red shortness in steels having as low as 0.032 per cent sulphur, whereas steels having higher percentages of sulphur did not exhibit red shortness. Here again the presence of certain undetermined ingredients manifest their presence. As a matter of fact, phosphorus and sulphur, which have been very much maligned in the past, are being revealed in more recent investigations to be not such bad actors after all, and the detrimental effects popularly accredited to them have been found to be due to other undetermined constituents. The reason for such past deductions have been generally due to the fact that certain harmful practices in steel making which resulted in the presence of the really harmful ingredients, also generally result in the phosphorous or sulphur contents being somewhat high, so that these elements have taken the blame for lack of better evidence.

Before considering the effects of the common and less commonly determined constituents on the properties of steel, it might be advantageous to discuss first the various types of steels and point out briefly certain fundamental differences in their physical structure and chemical make-up.

TYPES OF STEELS

All steels can be classified as one of three general types according to the method by which the steel was finished in the Open-Hearth. These three general classifications

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are: (1) fully deoxidized or killed steels, (2) semi-deoxidized or semi-killed steels and (3) rimmed or "open" steels.

KILLED STEELS

Killed steels or fully deoxidized steels, as the name implies, have been deoxidized by the addition of suitable deoxidizing agents in the furnace and ladle so that the steel when poured into the molds lies quiet or "dead." Failure to completely deoxidize this type of steel may result in subsurface blow-holes which roll out to form fine seams. Killed steels when properly made are a high grade product of uniform quality, both chemically and structurally. Needless to say such steels are more expensively produced due to the extra necessary precautions concerning both equipment and the deoxidizing agents.

For many purposes only killed steels are suitable, particularly where physical and chemical uniformity are required, and also where McQuaid-Ehn grain size control is practiced. For certain types of hot dip galvanizing, silicon-killed steels are demanded. Ordinarily a good quality killed steel will possess a superior outer surface, particularly in the higher carbon grades.

In so far as the ultimate analysis is concerned, killed steels are indicated by the silicon analysis which ranges from 0.15–0.30 per cent. For semi-killed the silicon content ranges from a mere trace to 0.12 per cent, depending on the amount of silicon added. Rimmed steels, due to their higher iron oxide content, never contain more than a trace of silicon and then only as SiO_2, approximately 0.02 to 0.03 per cent Si maximum. Even here we find important differences resulting from different materials and methods for deoxidation. The ordinary routine analysis, the so-called ultimate analysis, fails to reveal or indicate such differences.

When properly made, killed or fully deoxidized steels are free from oxides and gases, and solidification in the molds are accomplished without the formation of internal gas pockets or blow-holes.

SEMI-KILLED STEELS

Partially deoxidized or the semi-killed steels represent a considerable portion of the steel made today. This is due, mainly, to their low cost of production, being much less expensively produced than either the killed or rimmed steel varieties. Due to a suppression of the mold action by a fairly rapid solidification of the top of the ingot, the ingot possesses blow-holes, some fairly deep seated and others just below the surface of skin. In good practice these blow-holes may be deep seated enough not to break through to form seams or an otherwise scabby, "burned," surface. Due to the effect of the blow-holes the amount of pipe is substantially reduced resulting in blooming mill yields as high as 90 per cent as compared to much lower yields of 75 to 85 per cent with killed steels.

Ordinarily, semi-killed steels are inferior to killed steels with regard to surface and other physical properties in general. However, for a great number of uses this is not of utmost importance and the semi-killed variety finds considerable application in such fields.

Regarding the chemistry of semi-killed steels, it may be said that the ultimate analysis is the same as killed steels except for the silicon content which, as previously indicated, is lower, ranging from a trace to 0.10 to 0.12 per cent. Ordinarily this practice is confined to fairly low-carbon steels, usually under 0.25 per cent in which case the seams are reduced in magnitude when large reductions are made from the ingot.

In this practice, however, no prediction as to rolling qualities can be made with certainty. Many heats showing no cracks at the blooming mill rolls show numerous smaller cracks upon upsetting samples of the billets. Quite often a heat of steel will not only roll well but will show only very few cracks upon upsetting the billet. These latter heats with such desirable characteristics, not revealed in *the usual chemical analysis*, possessed a superior type of "skin" which resulted from certain practices on the Open-Hearth Furnace floor. Although such heats are of a semi-killed type, certain practices can be taken to produce fairly deep seated blow-holes with just enough action to eliminate excessive piping.

RIMMED STEELS

Rimmed steels are so made as to produce a characteristic mold action termed rimming. This rimming action is the result of the reaction of carbon and iron and manganese oxides, and results in the formation and evolution of carbon monoxide. This strong evolution of CO and other gases such as nitrogen and hydrogen interferes with the crystalline growth to the extent that instead of forming coarse columnar grains, a tough ductile skin or rim is formed instead. At the same time the gases are swept upward and out without being trapped as gas pockets or blow-holes. This action continues until a fairly thick skin has formed after which the gas becomes trapped and forms a layer of deep seated blow-holes.

The rolling quality of such steel depends upon the thickness of the skin, which in good practice is soft, ductile and entirely free from surface defects such as seams or cracks. However, not all rimmed steels have the desirable qualities named above since the depth of the gas holes depends on many variables which must be carefully controlled.

Rimmed steels are ordinarily of the low-carbon variety seldom exceeding 0.15 per cent, although higher carbon steels can be successfully rimmed by means of special equipment and technique. In steels containing higher carbon and manganese contents the mold action is considerably decreased due to the quieting or killing effect of these two elements and, therefore, decreases the thickness of the skin. The manganese content is usually under 0.40 per cent.

Rimmed steel with its soft thick skin over a relatively impure core enjoys numerous advantages and applications by virtue of the ductility and deep drawing properties of this outer surface.

While not included in the category of steels, the use of the ultimate analysis in determining the relative quality of wrought iron deserves mention. The carbon and manganese contents of high grade wrought iron are usually under 0.10 per cent, higher percentages usually indicating incomplete refining or adulteration with steel scrap. Due to the presence of characteristic longitudinal slag stringer, the silicon and phosphorous contents are fairly high ranging from approximately 0.10 to 0.20 per cent for each. Lower or higher amounts of silicon usually indicating abnormal slag distribution. Sulphur is usually kept under 0.04 per cent. However, in addition to the ultimate analysis, the microscope is an invaluable aid in evaluating the relative quality of wrought irons since their physical properties are greatly influenced by existing microstructure.

Up to this point we have seen that the ultimate analysis defines only in a general way the grade of steel we have under consideration. The analysis, assuming it

to be normal, will determine its general field of application. Also, we can determine the type of steel we have—that is, whether it is a killed or partially killed steel. Other than such general distinctions, no real clue as to metallurgical soundness is revealed. Relative quality between various steels of the same type, except in extreme cases, is not positively indicated by the ultimate analysis figures. The reason for this is quite obvious in that the chemical analysis is only one of the many tools by which we determine the quality of a steel. Physical testing, metallography, physics and radiography all have their place in the more complete studies of the behavior of metals.

FUNCTIONS OF THE CHEMICAL CONSTITUENTS OF STEEL

The effect of carbon on the hardness, strength and ductility of steel is too well known to bear repetition here. The carbon results chiefly from the pig iron additions and to a lesser extent on the scrap, depending of course on its carbon analysis, and the ladle additions. The practice of using coal or coke as a substitute for pig iron is not generally considered good practice for producing high quality heats. It has been observed, also, that heats recarburized with coal or coke in the ladle showed on the average inferior physical properties, although the final ultimate analysis appeared to be satisfactory. The reason for such unexplainable discrepancies is due to the fact that the Open-Hearth practice as regards the type of charge, the working and the methods of deoxidation determines to a large degree the kind and amount of the undetermined constituents present which also have their say in determining what the final physical characteristics shall be.

Manganese is added for the purpose of deoxidizing the steel by combining with oxygen present as FeO, for combining with the sulphur present to form insoluble and relatively harmless MnS, and for the beneficial effects on rolling qualities and physical strengths. The manganese content in plain carbon steels seldom exceeds 1 per cent above which value the steels have a tendency toward brittleness.

In certain classes of steel low-manganese content generally indicates inferior quality in so far as finish is concerned, this being particularly true of the semi-killed variety. On the other hand, extremely low-manganese contents are characteristic of high-quality wrought iron and some types of commercially pure iron.

Red shortness is prevented to a considerable extent by manganese which, as already indicated, tends to eliminate the ill effects of oxygen and sulphur in steel.

Sulphur is ordinarily an undesirable element in steel since it is usually associated with red shortness, and therefore is held to certain limits, 0.045 to 0.055 maximum. In well-made steels the relative percentages of sulphur when held to below the maximum limits mentioned, do not indicate relative quality because the manganese present has neutralized, so to speak, the harmful, effect of sulphur. The MnS particles which are formed have a negligible harmful effect on the properties of steels for ordinary structural purposes.

In many cases, however, relatively high sulphur content may indicate increased oxygen content in the steel. While the exact effect is not completely known, it is thought that the sulphides of iron tend to hold oxygen as FeO in solution in the molten steel.

There are certain classes of steels to which sulphur is purposely added for the specific purpose of promoting machinability. In this case increased amounts of manganese are present to assume the complete neutralization of the sulphur by forming large numbers of MnS particles throughout the body of the steel. These particles break up the continuity of the steel to the extent that small chips are readily formed in machining, producing a smooth surface. The sulphur in such steels is added in the ladle so that the steel is "resulphurized." The attendant ill effects of sulphur in resulphurized steel is not observed to the same extent as when the high sulphur contents are residual, that is, the result of impure scrap in the charge.

The use of phosphorus as a beneficial alloying element has received considerable attention in recent years. The advent of the low-alloy steels have shown the presence of phosphorus up to 0.150 per cent to be desirable. This is far in excess of the usual "0.045 max." limit which most specifications put on the phosphorous content. In the manufacture of thin, high-strength strip made from plain carbon steels, the use of phosphorus in contents ranging from 0.055 to 0.075 per cent has been practiced for a long time with highly satisfactory results.

Here again we have the case of residual phosphorus versus "added" phosphorus. In the cases mentioned above, the phosphorus is added in the ladle like any other alloying element and the resulting product does not exhibit the expected cold shortness, but instead usually has good ductility.

Rephosphorized steels are usually high-grade steels with the residual phosphorous content before rephosphorizing being quite low. On the other hand, high phosphorous contents resulting from residual phosphorus indicate faulty Open-Hearth practices. In such cases, the steel is of definitely inferior quality in many respects. But below the usual maximum limit of 0.045 to 0.050 per cent such comparisons or assumptions cannot be made. Numerous instances have been observed where steels of somewhat higher phosphorous content have proved superior to steels of lower phosphorous percentages, the other elements being substantially the same.

Silicon is added to the molten steel previously for purposes of deoxidation. It is always used, however, together with manganese in various combinations depending upon the particular plant practice. As previously mentioned, its content in killed steel usually ranges from 0.15 to 0.25 per cent. In semi-killed and rimmed varieties the silicon content is considerably less and is usually not indicated in the routine analysis report.

SEGREGATION IN STEEL

Without discussing the mechanism of segregation, it is sufficient to say that segregation occurs to some extent in every cast steel ingot regardless of the type of steel. Such segregation is confined mainly to carbon, sulphur and phosphorus. Manganese and silicon show decreased segregation tendencies.

In fully deoxidized or killed steels the tendency to segregate is much less pronounced than with semi-killed or rimming steels where there is considerable action in the mold tending to move the lower melting impurities toward the center of the ingot. Mold size is also an important factor since the larger sizes stay "open" longer. Small ingots solidify more rapidly allowing less time or chance for pronounced segregation to take place.

The analysis of the heat sample represents the average composition of the steel well enough if the sample has been obtained in a proper manner. However, when drillings are taken from some finished sample, the location with respect to the portion of the ingot should be known because of the possibility of encountering segregation, in which case the analysis figures would be very

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The analysis of the heat sample represents the average composition of the steel well enough if the sample has been obtained in a proper manner. However, when drillings are taken from some finished sample, the location with respect to the portion of the ingot should be known because of the possibility of encountering segregation, in which case the analysis figures would be very

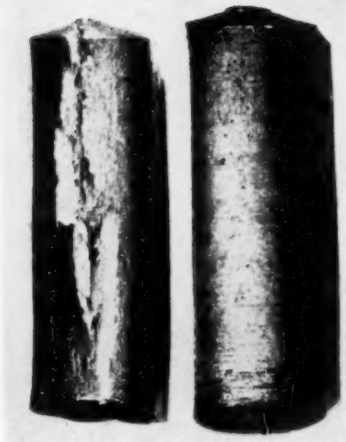

Fig. 1—(Left) Shows Cracking When Bent 180° Parallel to the Direction of Rolling

Fig. 2—(Right) Shows the Same Material Bent Across the Axis of Rolling

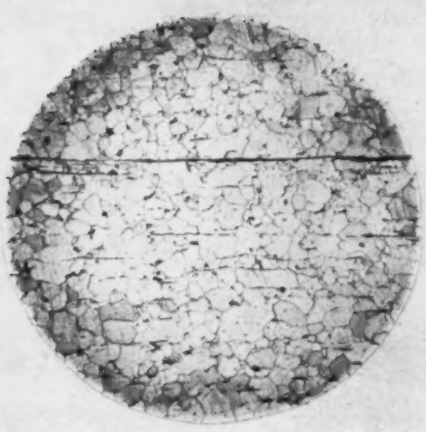

Fig. 3—X150, Shows a Longitudinal View of the Steel Shown Above. Note Excessive Amount of Non-Metallic Matter in the Form of Long Threads or Strings

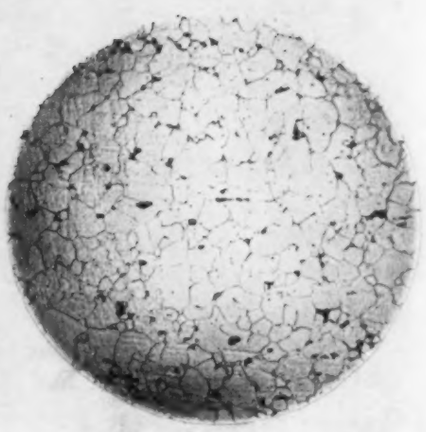

Fig. 4—X150, Shows a Cross-Sectional View of the Same Steel. Note the Ends of the "Strings" of Non-Metallic Matter Shown in Fig. 3

misleading. The possibility of encountering segregation is much more likely in the case of semi-killed and rimmed steels.

ALLOY CONTAMINATION IN STEEL

Due to the fact that a considerable proportion of every Open-Hearth charge is composed of various kinds of scrap steel, it is not surprising that many other elements are found alloyed with the steel being produced. These "contaminating" elements result from the various alloyed steels in the scrap charge. Whenever non-ferrous materials are known harmful alloys can be separated from the scrap, every effort is usually made to do so, nevertheless, large amounts occasionally are brought into the furnace with disastrous results. Not infrequently, the erratic behavior of steel of seemingly good ultimate analysis can be traceable to certain undesirable non-ferrous elements present in unusually large amounts. Tin, nickel, copper, chromium and vanadium are among the principal contaminating alloys to be found in scrap material.

The alloy contamination varies not only from heat to heat when different grades of scrap are used, but also varies from plant to plant, due to different sources of scrap materials.

While ordinarily the alloy contamination is quite low and for many purposes does not affect steel quality, yet for many other special applications excessive alloy content is definitely harmful.

The ultimate analysis is, in this respect, very incomplete, and the assumption that steel is composed only of the four or five elements which have been determined, often results in considerable embarrassment to all concerned whenever such material fails to measure up to the expected quality.

INCLUSIONS IN STEEL

Inclusions in steel may result from many different sources, such as the oxidation of the steel itself, products of deoxidation which have not been eliminated, particles of slag trapped within the body of the steel and to a lesser extent loose or eroded refractory material from the furnace or ladle linings washed into the stream of molten metal. The cleanliness of steel is therefore a relative quantity depending upon the amount, type and distribution of the inclusions. The particular usage of a steel must also be considered in judging the importance of such relative and ill-defined distinctions as "clean steel" or "dirty steel." It is not difficult to conceive and it is certainly entirely possible to have a "dirty" steel which at the same time had a very thick skin with no surface blow-holes and still have good rolling or forging quality. Whereas on the other hand, it could easily be possible to have a very clean steel but with a very thin skin over an excessive number of blow-holes near the surface and obtain a great deal of cracking.

The effects of large inclusions can be described as being definitely harmful and also in instances of pronounced segregation due to rolling. Figures 1 and 2 show samples of the same low-carbon flat material bent parallel and across the axis of rolling—in other words, parallel to and across the grain. The bends parallel to the grain showed poor ductility, due in part to the weakening effects of an excessive amount of inclusions segregated in planes as shown in Figs. 3 and 4.

In regard to the very fine inclusions, particularly where the amount is not excessive, it is sometimes difficult to find any impairment of quality, although in such cases the machinability is occasionally lowered due to more than average wear on the dies or cutting tools. It has been further postulated by many investigators that the fine particles, when of a particular type and amount, retard grain growth of the austenite, giving rise to the so-called fine- and coarse-grained steels as measured by the McQuaid-Ehn test.

McQUAID-EHN GRAIN SIZE

The effects of inherent austenitic grain size on subsequent steel treating operations have received considerable attention in recent years. It can be stated briefly that this inherent grain size affects the behavior of steels in all subsequent working and heat-treating processes. Steels vary in their inherent grain size from coarse, through various stages to fine, ranging in their A. S. T. M. classification from 1 to 8. Figure 5 shows a coarse grained steel according to the Standard McQuaid-Ehn test while Figs. 6 and 7 show a medium and fine grain steel, respectively.

Table 1

Fractional Extraction of Oxides and Gases in Different Typical Ferrous Materials

Temp. of Extraction Deg. Fahr.	Probable Oxides Reduced	Percentage of Oxygen	Hydrogen	Nitrogen
1. Semi-Rimming Plate				
1960	FeO	.....	0.00024	0.0011
2140	MnO	0.0095	.....	0.0022
2410	SiO_2	0.01075	0.00008	0.0011
2860	Al_2O_3	0.0057	0.00016	0.0011
Total		0.02595	0.00048	0.0055
2. Shielded Arc Deposit				
1960	FeO	0.0011	0.00014	.....
2140	MnO	0.0172	0.00014	.....
2410	SiO_2	0.0521	0.00056	0.0049
2860	Al_2O_3	0.0071	.....	0.0010
Total		0.0775	0.00084	0.0059
3. Bare Weld Metal				
1960	FeO	0.0323	0.00057	0.0970
2140	MnO	0.2395	0.00014	0.0534
2410	SiO_2	0.0085	0.00036	0.0386
2860	Al_2O_3	0.0266	.....	0.0010
Total		0.3069	0.00107	0.1900
4. Open-Hearth Iron				
1960	FeO	0.04668	0.00019	0.0092
2140	MnO	0.00904	0.00056	0.0013
2410	SiO_2	0.00527	0.00037	0.0026
2860	Al_2O_3	0.01205	0.00066	.....
Total		0.07304	0.00178	0.0131

It has been amply demonstrated that certain advantages are enjoyed by each class of steel, that is, the coarse and fine grained steels. These have been described in considerable detail in recent literature and will not be discussed here. It is sufficient to say, however, that for many purposes it is extremely important to know which type of steel one is dealing with. Such information is not indicated by the ultimate analysis figures so that here again we find an instance where additional data are necessary in order to ascertain the suitability of a certain steel for a particular purpose.

Grain size control consists of a carefully supervised technique involving the conditions peculiar to the individual plant. The proper use of certain strong deoxidizers including aluminum as one of the principle ingredients plays an all-important rôle. Fine grain canot be produced in the semi-killed or rimmed varieties of steels.

While ordinarily the coarser grained steels tend to show normal structures and the finer grained steels the more abnormal structures, it is easily possible to have an abnormal coarse grain particularly in a semi-killed steel. Also, the finer grained varieties may be highly normal. The degree of normality or abnormality seems to be associated with the amount and form of oxygen present in the steel.

GASES IN STEEL

In recent years the effects of various gases on the properties of steel have been studied by many investigators. It is quite evident now that the presence of certain gases in steel definitely influences its behavior and, therefore, accounts for hitherto unexplained differences in the properties of various steels.

While many methods for determination of gases, particularly oxygen, have been evolved, that of hot extraction seems to give highly satisfactory and reproducible results. Of particular interest and as an example of the importance of the determination of such analyses, several examples will be cited from the work of investigators in this field.

In his recent paper, "A New Tool for the Control of Quality Steel Making," presented at the Metal Congress in Cleveland, G. T. Motok[1] described the use of a new improved equipment for the fractional vacuum extraction of gases and oxides. He cited the fact that ordinary routine chemical analyses do not account for certain differences in the properties of steels and that different types of oxides, even though in very small amounts, may profoundly affect their efficiency in service.

Having obtained permission to reproduce data from Mr. Motok's paper,[1] we shall quote such examples which are of particular interest. The data in Table 1 shows the result of a fractional extraction of oxides and gases in different typical ferrous materials.

Additional data shown in Table 2 concerning the impact strength of two high-carbon steels were found to be affected by the oxide content. In this particular case the ordinary chemical analysis and the McQuaid-Ehn grain structures were found to be practically the same. Heat 60 was inferior to heat 73 in impact strength.

Of two welding rods, one of which was described as being "bad," it was found that the latter contained greater amounts of oxides, particularly Al_2O_3. These data are reproduced in Table 3.

Difficulty in machining, that is, in obtaining satisfactory tool life, is quite often unexplainable on the basis of the ultimate analysis or its existing or inherent grain structure. It is believed that numerous hard nonmetallic inclusions have a detrimental abrasive effect on cutting tools. Motok shows the relationship between tool life and oxides of the Al_2O_3 and SiO_2 type as being probable factors in causing the shortened tool life in machining heat 58 (Table 4).

Table 2—Impact Strength as Affected by Oxide Content

Temp. of Extraction Deg. Fahr.	Probable Oxides Reduced	Heat 73			Heat 60		
		% O_2	% H	% N	% O_2	% H	% N
1960	Free O_2 and FeO	0.00363	0.00094	.....	0.00129	0.000054	.....
2140	MnO	0.0076	0.000082	.....	0.00215	0.000054	0.00075
2410	SiO_2	0.0142	0.000205	0.00805	0.00430	0.000268	0.00075
2760	Aluminum Silicate	0.0399	0.000327	0.01435	0.01634	0.00107	.....
3000	Al_2O_3	0.02605	0.000511	0.00287	0.09933	0.000589	0.00449
Total		0.09138	0.002085	0.02525	0.12341	0.002015	0.00599

[1] Tables 1 to 6 reproduced by permission of the American Society for Metals.

Fig. 5—X100, Shows a Coarse Grain Steel According to the McQuaid-Ehn Test

Fig. 6—X100, Shows a Medium Inherent Grain Size

Fig. 7—X100, Shows a Fine Inherent Grain Size

Table 3

Welding Rods

Temp. of Extraction Deg. Fahr.	Probable Oxides Reduced	Percentage of Oxygen	Hydrogen	Nitrogen
Good				
1960	FeO	.....	.....	.....
2140	MnO	0.0126	0.00064	0.0020
2410	SiO_2	0.00285	.....	0.0010
2860	Al_2O_3	0.00228	0.00057	.....
Total		0.01539	0.00121	0.0030
Bad				
1960	FeO	0.00352	0.00021	0.0051
2140	MnO	0.00880	0.00048	.....
2410	SiO_2	0.00704	0.00027	0.0061
2860	Al_2O_3	0.04400	0.00052	0.0041
Total		0.06336	0.00148	0.0153

Differences in the characteristics of two spring steels, in which spring "B" was far superior to spring "A," was shown to be influenced by the amount of gases and oxides in the steels. Steel A contained considerably greater amounts of gases and oxides as shown in the data reproduced in Table 5.

Two steels of similar chemical analysis showed considerable difference in depth of hardening on quenching, one being shallow whereas the other showed deep hardening. Ordinary routine tests failed to reveal any material difference between the two steels. Vacuum extraction indicated higher Al_2O_3 content to be an inhibiting factor in deep hardening (Table 6).

The foregoing examples indicate quite clearly the importance of the little determined constituents on the quality of steel, and that abnormal or unusual behavior in steels is not always indicated by their ultimate analysis, but rather on the analysis of constituents rarely determined. It is unfortunate that the complexity and cost of these methods have been retarding factors in the more widespread adoption of such determinations.

Table 4

Pieces Per Tool Grind versus Type of Oxide in Steels of Same Chemical Analysis

Temp. of Extraction Deg. Fahr.	Probable Oxides Reduced	Percentage of Oxygen	Hydrogen	Nitrogen
Heat 61—65 Pieces Per Tool Grind				
1960	FeO	0.00145	0.00018	.....
2140	MnO	0.00182	0.00009	0.0038
2410	SiO_2	0.00145	0.00018	0.0006
2900	Al_2O_3	0.00435	.....	0.0057
Total		0.00907	0.00045	0.0101
Heat 58—30 Pieces Per Tool Grind				
1960	FeO	0.00168	.....	0.00735
2140	MnO	0.00126	0.00016	.....
2410	SiO_2	0.00504	0.00016	.....
2900	Al_2O_3	0.00924	0.00026	0.00735
Total		0.01722	0.00058	0.0147

CHEMICAL FACTORS AFFECTING THE WELDING QUALITY OF STEEL

Defects of a chemical nature affecting the quality of welded material could be included under the general heading of porosity which would include voids due to gases and non-metallic material of a slag-like character. Porosity due to gases and occluded non-metallic matter is dependent upon the nature and quality of the metal being welded and also upon the particular kind of welding rod. Since our remarks are not concerned with the relative merits of the various welding rods, we shall discuss only briefly a recent investigation on the quality of steel being welded. Wilmer E. Stine[2] devised a method of determining the relative quality in regard to porosity of the parent metal by using carbon arc welding with a shielded arc. In this manner the use of filler material and its accompanying variables was eliminated. It was found that steels high in silicon and aluminum produced porous welds whereas the effect of high manganese was not pronounced. Excessive amounts of silicon and aluminum result from the deoxidation of steel during its manufacture. Mr. Stine pointed out that in order to prevent rapid absorption of gases at welding temperatures, the silicon, aluminum and manganese contents should be kept as low as possible. This is necessary since the gas holding capacity for steels, even in the solid state, varies directly with the amounts of these elements present. During welding, these elements are readily oxidized, thereby diminishing the gas holding capacity of the steel liberating gases which result in a porous weld.

Table 5

Vacuum Extraction of Gases and Oxides Occurring in Two Spring Steels

Temp. of Extraction Deg. Fahr.	Probable Oxides Reduced	Steel "A" % O_2	% H_2	% N_2	Steel "B" % O_2	% H_2	% N_2
1960	FeO	0.00908	0.000936	0.00590	0.00259	0.000016	.....
2140	MnO	0.01259	0.000855	0.00169	0.00246	.....	0.00823
2410	SiO_2	0.01945	0.000842	0.00212	0.00621	0.000016	.....
2760	Aluminum Silicate	0.02404	0.000536	0.00853	0.01200	0.000143	0.00245
3000	Al_2O_3	0.03060	0.000324	0.00908	0.01030	0.000115	0.00338
Total		0.09576	0.004293	0.02732	0.03356	0.000290	0.00606
Chemical Analysis:	Aluminum		0.019			0.079	
	Alumina		0.016			0.019	

Table 6

Deep Hardening Steel

Temp. of Extraction Deg. Fahr.	Probable Oxides Reduced	Percentage of Oxygen	Hydrogen	Nitrogen
1960	FeO	0.00163	0.00047	0.00095
2140	MnO	0.00109	.....	0.0019
2410	SiO_2	0.00163	0.00020	0.00855
2860	Al_2O_3	0.00217	0.00013	0.0019
Total		0.00652	0.00080	0.0133
Shallow Hardening Steel				
1960	FeO	0.00054	0.00020	0.00185
2140	MnO	0.00107	0.00027	0.0028
2410	SiO_2	0.00160	0.00033	0.00374
2860	Al_2O_3	0.00375	0.00027	0.00374
Total		0.00696	0.00107	0.01215

SHORTCOMINGS OF THE ULTIMATE ANALYSIS

It should be obvious from the foregoing that the routine ultimate analysis is incomplete. The undetermined constituents may be either beneficial or harmful to the physical properties, depending of course, upon the amount present and the particular requirements of the steel. For many purposes the presence of certain constituents could not be regarded as harmful whereas for other more specialized uses these same constituents may exert a noticeable harmful effect. In other words, harmful alloy contamination from scrap charged into the furnace, excessive amounts of non-metallic inclusions—so-called "dirty steel," gases and gas holes, segregation in the ingot and any number of physical defects—all of these which are the real factors in determining the quality of steel are not indicated in the simple ultimate analysis. Neither are the inherent or McQuaid-Ehn grain characteristics which play an important rôle in many steel treating operations, revealed in the ordinary routine analysis data.

The methods used in the deoxidation of a heat of steel have a profound influence upon resulting steel quality, yet the simple analysis does not distinguish between steels produced by these various methods, some of which are costly but produce a high quality product, and others which are much less costly, producing a far inferior grade of steel—yet which may have the same or in some cases a better ultimate analysis.

In many cases the size, form and distribution, as well as the chemical nature of the constituent is of importance. It is quite possible to have many combinations of the constituent elements as well as the familiar variety of iron and carbon combinations which are both physical and chemical in nature.

For the complete study one must necessarily avail himself of allied fields of research such as physical testing, metallography and radiography.

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2. Stine, W. E., "Factors That Affect the Welding Qualities of Steel," *Yearbook of The American Iron and Steel Institute*, **1931**, 486.

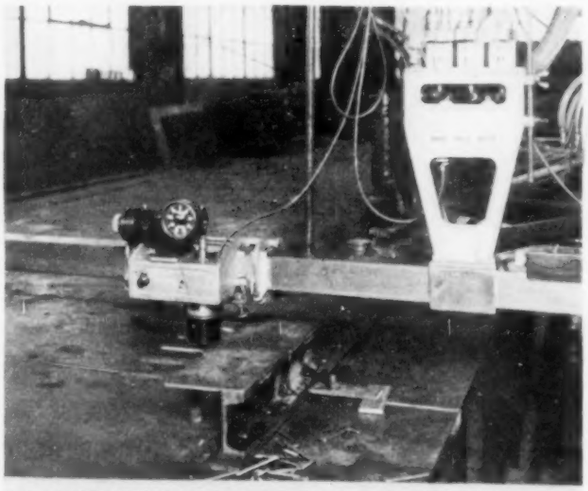
FLAME CUT 44-FOOT EXPANSION Joint for Blue Island Bridge

By R. J. TRAUTMAN†

MODERN bridges require a tremendous tonnage of flame cut steel, and bridge companies have found that the parts which present the greatest problem are those sections of the assemblies termed the expansion joints. The Wisconsin Bridge & Iron Company, informed us that they had received a contract for the construction of the Blue Island, Cook County Bridge, over the Calumet Sag Channel, and that the prints called for an expansion joint forty-three feet, ten and one-half inches long—*in one piece*. The expansion joint described was of copper steel alloy stock, fifteen inches wide and one and one-half inches thick, which, because of its design required a continuous zig-zag cut one-hundred fifty-six feet, three inches in length. A further requirement specified that the kerf be one-quarter of an inch wide to allow for expansion in the section after it was installed as a part of the bridge.

† Sales Correspondent, Milwaukee District, Air Reduction Sales Company.

Fig. 1—Magnetic Tracer and Method of Attaching Sectional Template



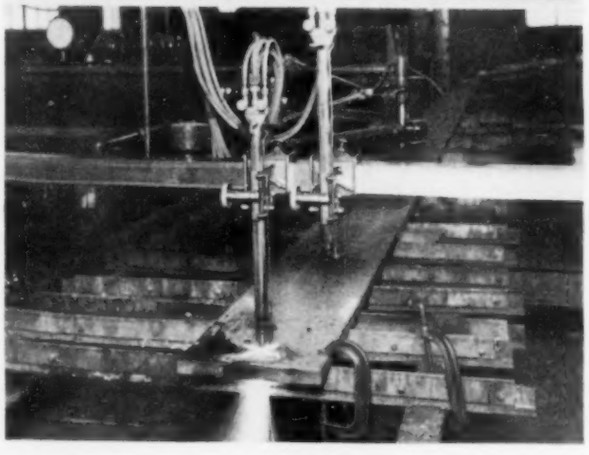


Fig. 2—Starting the Cut

Fig. 3 Wet Rags Help Cool the Work

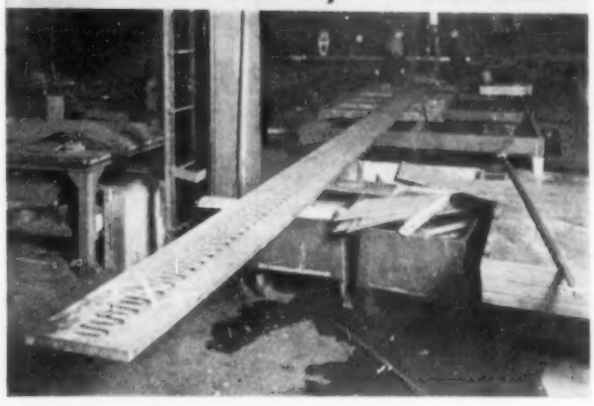


Fig. 4—Machine Cut Completed

Because of the length of the cut, and the peculiar conditions to be met, it was found necessary to spend a great deal of time in preliminary and layout work to eliminate the possibility of any difficulties when the actual cutting began. It was decided to use the Magnetic Tracer and a template made up in two movable sections so that continuous cutting could be arranged. Figure 1 shows the magnetic tracer and method of attaching the sectional template to the guide bolted to the tracing table. This was done by fitting the pins in the template bases into the holes prepared in the guide. Each of the two sections of the template was about eight inches long; the

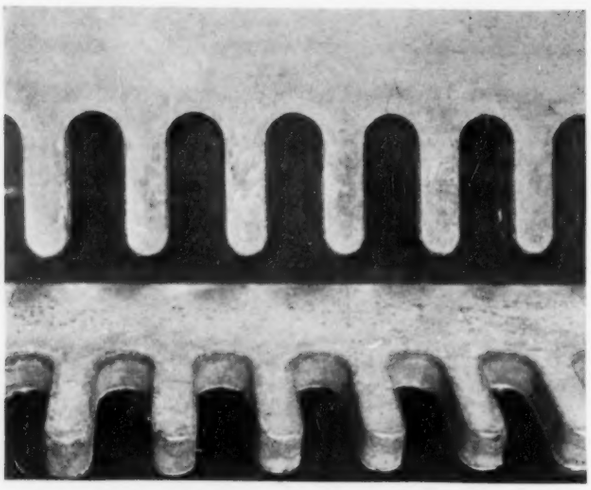


Fig. 5—Close-Up View of Finished Cut

sections were moved ahead as the cut progressed. The drilled steel guide rail used with the templates was twelve feet long and, therefore, it was necessary to move it ahead four times during the course of the work as the cutting progressed; the slab only required moving once.

A special tip was developed by enlarging the oxygen orifice of a standard style cutting tip with a No. 56 drill. The tip so obtained worked out perfectly as it produced the $^1/_4$-inch kerf specified and did not cause trouble with excessive preheat. The forty-three foot slab was first lined up with tracing table. Eight steel braces were fitted tightly against the slab and then welded to the cross pieces of the skeleton work table.

The braces served a double purpose in that they kept the steel from expanding and warping in its width and acted as a checking point against any lengthwise "crawl" due to expansion during the cut. "Crawl" was checked by placing soapstone markings on the cold slab at the brace before beginning the cutting and periodic inspections of the markings were made as the cut progressed to ascertain whether or not there was any tendency to "crawl" on the part of the slab.

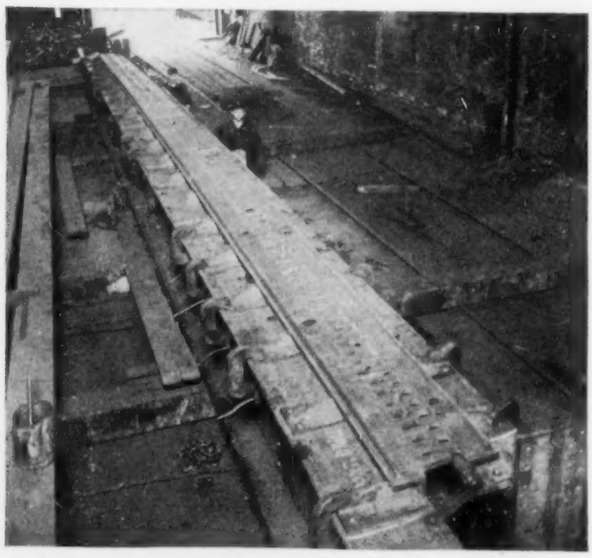


Fig. 6—Top View of Completed Job

The cut was started from a hole $^5/_{16}$ inch in diameter about one and one-quarter inches from the leading end of the slab as shown in Fig. 2. As the cut progressed, $^1/_4$-inch steel pins were inserted in the kerf at about 4-inch intervals to keep the proper width between the teeth of the joint. Clamps were adjusted across the width of the steel. An additional step taken to eliminate warping consisted of following directly back of the torch with water soaked rags. It was found that this cooled the metal and helped to minimize warping. Figure 3 illustrates this portion of the work. The cut was ended in the same manner in which it was started by shutting off the torch and stopping the machine at a point about one and one-quarter inches from the end of the stock. The completion of the machine cut together with the clamping arrangement used as shown in Fig. 4.

As soon as the slab was cooled, the cutting machine was started again from the point at which it had stopped and the cut was completed; the cut at the starting end was completed by cutting from the edge to the starting hole with a hand torch. The clamps were then removed and it was found that warpage had been held to 4 inches. The slight warpage was easily corrected in a "bulldozer."

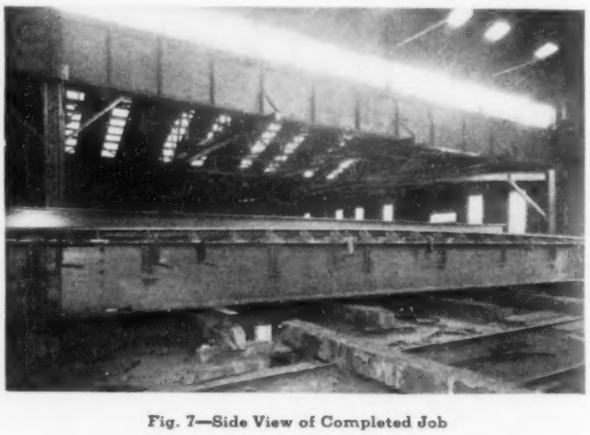


Fig. 7—Side View of Completed Job

Because of the extreme length of the stock to be cut, it was necessary to move the job ahead once, but the cutting range of the new machine was such as to eliminate the tremendous amount of handling which would have been necessary with other shorter cutting machines. The cut turned out had an unusually fine finish in spite of the quarter inch kerf specified by the customer; the slightly rolled edge obtained was exactly what the customer needed as it saved machining later. Close-up view of the finished cut is shown in Fig. 5. Various cutting speeds and various pressures were used and finally we arrived at an efficient speed of five inches per minute with the high-pressure oxygen set at thirty-five pounds and the preheat pressure set at five pounds on oxygen and five pounds on acetylene. It was found that at the above speed and pressures, the quarter-inch kerf was best maintained when the preheat flames were adjusted to a point $^3/_{16}$ inch away from the base material. Two men were required to do the actual work connected with the cutting and the total cutting amounted to seven hours. During this period 2900 cubic feet of oxygen were consumed together with 407 cubic feet of acetylene. The work was done by the Heil Company.

FIELD PRACTICE IN WELDING

Oil Well Casing*

By HOWARD L. NEWBY†

THERE is great need of closer coordination between the different parties involved in completing a successful installation of welded casing—the manufacturer of the casing, the purchaser and the welder.

To state the problem briefly, we are required to join together by welding, two vertical places of pipe having diameters of from $5^{3}/_{4}$ inches to $18^{5}/_{8}$ inches or even larger; to carry the tensile stresses developed by a vertical string of a mile or more of such pipe; the compressive stresses developed by an equal depth of heavy mud; the impact of a drill stem within the pipe weighing 22 pounds per foot and revolving at 250 r.p.m.; to remain perfectly water and gas tight, under these stresses and pressures; to be smooth on the inside within $^{1}/_{32}$ inch and to have an outside diameter as little above the O.D. of the pipe as possible. In addition, we are expected to match up the ends of the pipe perfectly and to align a piece of pipe sticking 40 feet up in the derrick weighing perhaps a ton absolutely with the casing already in the hole. Bear in mind that this must be done with the maximum possible speed.

TENSILE AND COMPRESSIVE STRENGTH

Let us inspect these conditions a little more closely. $8^{5}/_{8}$-inch Grade C No. 43 casing is being set to a depth of 8000 feet at which depth it has a factor of safety of 2. The weight of such a string, if not floated in, is 280,000 pounds. The length of the neutral axis of the wall of this casing—the effectual length of the weld if butt welded, is $25^{1}/_{2}$ inches. With a wall thickness of 0.487 there are 12.4 square inches of weld-metal resisting this weight, developing a stress of 22,600 pounds per square inch. With the welding rod now available this is not high enough to cause serious concern, but this strength is not nearly enough. Not only must we support the weight of this string of casing, but if for any reason it must be pulled out, the resistance of the vast surface of the casing against viscous mud is incalculable. Consequently, we *must* approximate the full tensile strength of the casing as closely as possible. Let it be interpolated right here that it is assumed that we are working with Grade C casing, although I have some very pertinent remarks to make on that subject a little later. There is casing now on the market that has a tensile strength approaching 90,000 lb. per square inch. This, then, is our goal, to give the driller a weld having a strength of 90,000 pounds if possible.

IMPACT STRENGTH

In the matter of impact opinions differ. Some consider it negligible, but we ourselves are considerably concerned about it. A joint in a drill pipe concentrating the weight of ten feet of the pipe at one spot and banging it against the weld 250 times per minute can develop some very real stresses. The driller may strike some very hard going and make only a foot or two of hole in two hours. That tool joint may have pounded a weld 36,000 times in that period! The blow is dampened to some extent perhaps by the mud within the casing but the outside of the weld may be either tight against hardest granite or supported only by mud.

Absolute tightness of the weld while under these stresses is, of course, a prime requisite. We are fortunate that with competent welders and the proper technique, the tightness of a multi-pass weld is assured. The greatest interest in this point lies in the fact that it is one of the prime weaknesses of the joint we are displacing—the screwed joint.

SMOOTH INTERIOR NEEDED

Now to enlarge on the matter of a smooth interior of the weld. The operator insists on a guaranteed utter absence of any icicles or dill-berries on the interior of the casing. He protests even more strenuously if the two ends of the casing do not coincide exactly. And that is indeed a real problem! A screwed joint is machined, and offers no restrictions in the pipe to hang up bits, start flaws in the casing or tear up rubber tool joint protectors. We must guarantee the same for welded joints.

Let us take a look at the matter of weld-metal intrusion in the case of the butt joint, the only type of joint in which it occurs. We must get complete penetration to develop full strength and to prevent concentration of stresses at a notched contour, yet have a minimum of metal on the inside, no more than about $^{1}/_{32}$ inch. Many schemes have been devised to accomplish this trick result.

We tried every method we heard of or could devise to accomplish the twin purposes of lining up the casing, both for concentricity and for straightness, and for securing 100% penetration without any weld-metal projecting inside. We finally hit upon a method that fills the bill but has the weakness of requiring special skill and training on the part of the workman. We have a split clamp of sufficient strength to positively force the swinging piece of casing into alignment with that already in the hole. Four tacking ports are cut into the side of the clamp that give weld area sufficient to hold the casing firmly in position when the clamp is removed. But, in addition, there is a wedge in the interior of the clamp that fits into the beveled edges of the casing. It is so proportioned that when the clamp is fully tightened the wedge actually separates the two pieces of casing. By careful experimentation we have found a welding gap, a ratio of rod diameter and welding heat and a technique of handling the welding rod that

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† American Pipe & Steel Corporation, Los Angeles, Calif.

permits a trained operator to lay a bead in a horizontal weld in a vertical pipe that has perfect penetration but which will freeze on the inside with no more than 1/32-inch internal bead. The surface tension of the molten metal in the rod we use, and the wettability of the parent metal by the molten rod are such that the interior bead is fairly wide but close to the metal and almost completely smooth, fulfilling the requirements. It is possible to cut open sample after sample and find over 90% of the interior circumference showing this interior bead, giving complete penetration and preventing any sharp concentration of stresses at a notch, the remaining ten per cent being through but not showing actual bead on the inside.

Concerning protruding edges of the casing in the sleeve type of joint or non-concentricity, A. P. I. standards permit 1% out-of-roundness. In 11 3/4-inch casing that is 1/8 inch. In practice it is very much less. In hundreds of measurements of seamless and welded casing we have seldom found out-of-roundness exceeding 1/16 inch. A properly fitting sleeve will force ends of such casing into proper position and minimize this difference in diameters and prevent any harmful protruding edges.

Single-belled joints, and double-belled joints with proper back-up bands offer no difficulty on this score, and there is, of course, no penetration of the weld into the interior of the pipe and no chance for dill-berries.

The exterior of the joint should be as small as possible. The entire well must, of course, be drilled to accommodate circulation outside the largest diameter of the casing to be set. 8 5/8-inch casing requires a bore of a certain diameter. If it has screwed joints with collars 9 5/8 inches in diameter the hole must be 1 inch larger. Sleeve joints and bell joints have a diameter approximately equal to screwed collars, but with butt joints, if the O.D. of the weld is kept at a minimum, a real advantage is gained. The total saving in time, bits, mud, fuel, etc., of 1 inch in diameter of all sizes of casing can run into thousands of dollars in a deep well. It has been conclusively proved that excess metal on a butt joint is no advantage as far as strength is concerned, is even a detriment. Proper welding technique can produce a weld in which the groove is entirely full yet the head will nowhere crown more than 1/8 inch. This is a real advantage in favor of the butt weld.

SPEED OF WELDING

As to speed in welding, let me remind some of you, not in daily contact with drilling procedure, that it is not a matter of welder's time only, far from it. A hole is drilled, let us say, 1000 feet without casing. It passes through hard and soft strata, clay, sand and gravel. The walls of this open hole are kept in place by means of a constant stream of heavy mud, circulated to remove the cuttings from the bit. In order to set casing, the drill-pipe must, of course, be removed and circulation stopped since there is then no means of carrying the mud to the bottom. It is of the utmost importance that the casing be run just as quickly as is humanly possible, to prevent the side walls of the bore from caving in. Welding personnel, methods and equipment have been improved to the point where slip joint casing is being regularly run in time that compares very favorably with screwed joints. In larger sizes of casing it is actually run in more rapidly than the same size of screwed pipe. In smaller sizes it is slower. If you will pardon the reference to our own files, I took the last ten jobs of 11 3/4-inch casing that we welded in and found 7716 feet of casing, 193 joints, run in 16 hours or 5 minutes per joint, 500 feet per hour. 13 3/8-inch casing took 5 1/2 minutes per joint. This is not welding time only, but it is total elapsed time including lowering away the completed joint, bringing in the next joint from the pipe rack, picking it up in the derrick, stabbing it and welding it. The actual welding time was about 2.3 minutes. We may say that at least in the larger sizes we can satisfy the driller as compared to screwed pipe.

At present, however, the actual welding time of a butt joint takes somewhat longer than the slip joint. One cannot use quite as much welding current, consequently the deposition of weld-metal per minute is less and at least as much metal is used. However, if properly designed clamps are used the handling time is not increased, and the over-all time is only about 20% more.

So much then for the conditions to be met in this job of welding. How about the materials we are working with, the casing. Two grades are in popular use in this territory—Grade C and Grade D. A. P. I. describes them as follows:

Grade C to have Mn between 0.35 and 1.50, tensile strength not less than 75,000 pounds and yield not less than 45,000 pounds. No other limitations.

Grade D, no restrictions on analysis, tensile 95,000 pounds, minimum yield 55,000 pounds.

Grade C formerly contained about 0.30 carbon and 0.45 Mn a fairly weldable steel.

PROCEDURE IN WELDING

I want to explain something of the actual procedure of making each kind of a joint as practiced in this field, beginning with the most popular, the slip joint. First, the sleeve itself. It is a cylinder, as truly round as possible, as close fitting as we dare to make it to secure concentricity of the two pieces of casing and long enough to act as a clamp in aligning the entering length of casing with that already in the hole. It must be weldable and of as high a tensile strength as possible. We buy this steel in strips, width equal to the length of the required sleeve and close chemical and physical limitations. You will pardon me if I do not give its complete chemical analysis, nor some of the procedure in making it truly cylindrical. We have found the tolerances that we must allow for different kinds, sizes and makes of casing, which governs the I.D. of the completed sleeve. We do, however, occasionally find a length of casing too much over size to properly enter the sleeve. Rather than make all sleeves to take this occasional diameter with consequent sloppy fit on a small diameter, we expand the sleeve by heating to take this oversized casing. Acetylene torches are part of the standard equipment both in putting the sleeves on the casing in the yard and completing the joint in the field. However, we have found by long experience what tolerance to allow to give a firm grip, a true seat and a concentric string of casing properly aligned.

After the material is cut to the proper length the laps are broken in the flat plate, they are rolled and tacked with the correct spacing between the edges to give good penetration to the weld. They are welded on a shielded arc automatic A.C. welding machine. The inside of the groove is then chipped to clean metal and a bead run inside by hand. This bead is ground off smooth and the sleeve put through a special rounding and straightening. The first heavy weld on the outside has pulled the seam up out of shape and the true contour must be restored. This completes the sleeve, ready for application to one end of each piece of casing. We send portable welding equipment to the manufacturer's yard and apply the sleeves there, using high tensile strength coated rod and building up a full fillet, being careful to get full penetration and sound weld-metal. Undercuts

are particularly obnoxious as not only reducing the cross-sectional area of the piece of casing that may be stressed practically to its elastic limit, but as cutting a notch clear around a piece of pipe that may be struck 36,000 heavy blows while under this heavy stress. Remember that in setting casing a factor of safety of only 2 is common practice and even less than that is often employed. *Every weld must be as nearly perfect in every respect as possible.*

There seems to be a tendency lately toward the bell end joint, both the bell and spiggot and the double bell with a back-up strip. In the bell and spiggot the bell is comparatively short. It is long enough to make it a lap joint, preventing dill-berries since the weld does not penetrate to the inside, and compelling the ends to be truly concentric and with a smooth interior contour, but not long enough to be of material aid in lining up the casing.

To get a truly straight joint several methods of lining up are available. One may merely use a straight-edge, notched out to clear the bell. This means a man on the stabbing board in the derrick—a dangerous place to work—and it means that not only must he be able to pull the upper end of the casing around as directed from the derrick floor to line it up, but he must hold it there in spite of wind or swinging block while it is being tacked, the very tacks themselves tending to pull it out of line.

Or the first joint may be so plumbed, and a fork set in the derrick so that each succeeding joint being fitted into the fork is likewise plumbed. This system also requires a man in the stabbing board, and it is difficult to quickly and firmly fix a fork in the derrick frame. It is in danger of being knocked out of position or even down upon the heads of those on the derrick floor by the swaying casing or the block and links. It must be hinged and swung out of the way when lowering the attached piece of casing.

We employ a clamp which we have devised that fits down over the bell with a funnel-shaped barrel that centers the middle of the clamp with the upper end of the casing already in the hole. At the lower end of the clamp is a self-centering chuck which, on being tightened, aligns the barrel of the clamp truly with that length of casing. This is all done while the next length is being brought into the derrick from the pipe rack, thus occasioning no loss of time. This new length is dropped through the upper half of the clamp and into the bell. When the upper centering chuck is tightened the new length of casing is truly aligned with that already in the hole. Tacking ports permit half of the circumference of the bell to be welded. The whole clamp is split and opens on hinges so that the clamp may then be removed while the balance of the weld is being layed on. This clamp has proved to be fast, accurate, and is made strong enough to force the casing into alignment against any wind pressure, or swinging of the heavy block and to hold rigidly while the tacks are being applied. I should perhaps, add that we have made application for patents on this device, as well as on the wedged clamp for butt-welding previously mentioned.

PERSONNEL

Concerning the personnel of the welding crew. One of the first requirements of the welder is oil field experience. Oil field workers have a language all their own, and you and all your men had better speak it. And don't feel that this is only to humor a stubborn driller, either, for things can happen very fast around an oil rig and your crew has to know what the crew is talking about. They have conditions to meet which are peculiar to their business and a welder who doesn't know what it is about can be a real handicap.

The second qualification of a welder is a direct derivative of the first, he must realize every minute the utmost importance of what he is doing, lowering 75 tons of steel hanging by his weld down into a hole which may have easily cost somebody $200,000, that it takes *speed* to get into the hole before it cases or freezes up; he must stand ready to cut the casing out if it sticks and has to be pulled out again, and he has to keep his head in any kind of an emergency.

EQUIPMENT

Now, some remarks about the equipment in use for this work. The first requisite is reliability. A drilling operator 150 miles away may phone in at any hour of the day or night that he is coming out of the hole at a certain time. He expects the welding crew to be there at that time regardless of anything, with equipment which will positively start that casing going down the moment the bit is out and keep it going in until the open hole is safely encased in steel. Excuses and accidents simply do not go—that hole must be cased. All welding companies, I believe, supply gasoline driven welding sets of ample capacity and on a deep hole supply an extra set for an emergency. In the case of an unusually long string an extra welder is furnished to spell off the men at work, but with casing being run at 500 feet per hour this is seldom necessary. 1/4-inch and 5/16-inch rod is commonly used and with lots of heat. 350 amps. can be handled by some welders without undercutting or danger of blowing a hole clear through the casing wall.

Cutting torches must be furnished immediately at hand, for if the hole caves and the casing sticks, it must be pulled out, cut into 80-foot stands and stacked in the derrick while the hole is reamed.[8] A big welding tip is also furnished with this equipment to expand by heating, any sleeve that is too close a fit to readily take an oversized piece of casing.

In addition to welding and oxyacetylene equipment it is common practice, at least on the larger sizes of casing, for the welder to furnish certain equipment for handling the casing. *An elevator*, which is fitted around the casing to lift it into the derrick the *links* which fit over the hook of the derrick block and engage ears on the elevator, a *clamp* tightened around the casing above the elevator for additional safety when the full weight of the entire string is taken in lowering away into the hole, and a *spider* with its *slips* which grip the casing in the hole and hold it while the block is disengaged picking up a new length to be welded on.

One other piece of equipment we use and deem of the utmost importance is a device for testing for explosive mixtures of gases on the derrick floor, in the cellar, and within the casing itself. We insist on its use before doing any welding and hold the driller responsible for giving us safe working conditions. If they do not prevail we do not strike an arc.

To sum up, welded casing in the Grade C quality is a success and is here to stay. Operators want it, and what the operator wants the steel company will make, and his distributor carry in stock. Important problems remain to be worked out in regard to being certain of weldable steel in the Grade C, and an immense amount of research must be successfully concluded before welding Grade D can be called a success. It is a big field for the welding engineer and one that the oil companies are watching closely. Screw pipe leaks under high stress and the joint efficiency seldom exceeds 65%. The welded joint is tight, joint efficiencies can be guaranteed to exceed 85% and in most uses is no more expensive than the screwed joint.

WELDED STRESSED SKIN PENDULUM Type Railway Passenger Car*

By ELIOT F. STONER†

THE purpose of this paper is to tell of the important part welding has played and is expected to play in the development of a unique new railroad car. Because the project is so new this paper cannot be entirely an account of what has been done, but instead will serve as an introduction to a new type of railroad car structure particularly well adapted to the most effective use of modern welding methods.

Before discussing the structural features and the intended applications of welding it will be well to describe briefly the reasons for the project and the results thus far obtained.

The underlying motive of this development has been to provide the railroads with a truly modern passenger car designed primarily to compete with other forms of transportation more favorably than is possible with any existing equipment. When one considers the fact that standard railroad passenger cars accommodating 60 passengers weighing 120,000 pounds or more (a weight per passenger of 2000 pounds) and that highway busses carrying 24 passengers weigh about 18,000 pounds (or 750 pounds per passenger) it becomes obvious that an effort to reduce railroad car weight would be extremely worth while.

However, merely reducing weight is not a complete answer to the railroad's problem of making money in the passenger-carrying business. Equipment which will completely solve this problem must satisfy two exacting customers; namely, the railroads and the travelling public. The railroads require equipment that is safe, reasonable in cost and economical to operate, and the travelling public demands riding comfort, low rates, fast schedules and pleasing appearance. Experience has shown that standard railroad equipment, including the new streamliners, cannot satisfy all these requirements.

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† Structural Engineer, C. T. Hill, Inglewood, Calif.

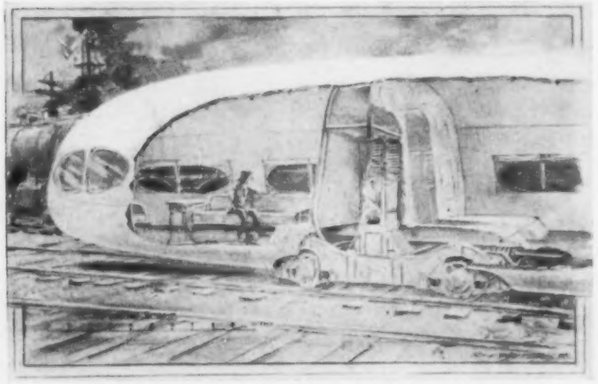


Fig. —Cutaway Section of Body Showing Rear Truck and Suspension Structure

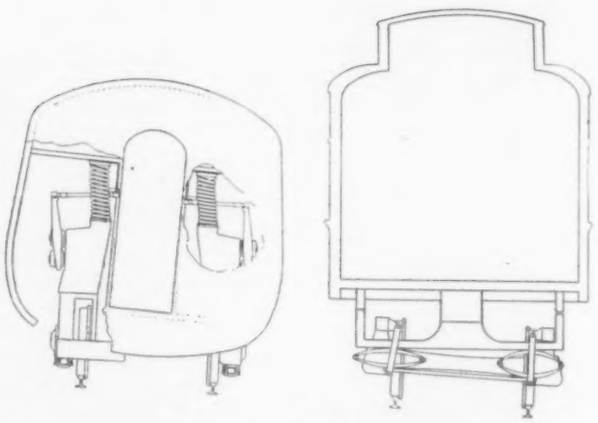


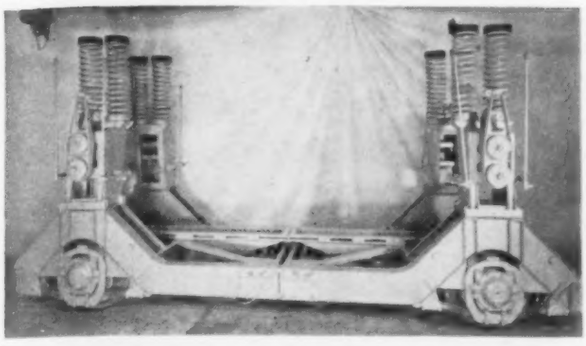
Fig. 2—Sectional Diagram of Standard and New Hill Car Trucks Showing Action of Bodies When Rounding a Curve to the Right

True, some progress has recently been made in the way of decreased weight, faster schedules, and improved appearance, but these improvements have only been accomplished at the expense of some of the other items.

In view of these considerations design of a car body, truck and suspension system was undertaken. The suspension system, or method of mounting the car on the truck, is a complete departure from standard practice. The car body is virtually suspended from the truck, floating on soft vertical coil springs mounted on towers which are a part of the truck as shown in Fig. 1. These springs are designed to allow horizontal motion of one end with respect to the other to permit all necessary truck motion relative to the body, such as swiveling on turns. These coils provide the vertical springing, but in a railroad vehicle lateral springing is even more important since the truck is effectively grooved to rails which frequently have as many lateral irregularities as vertical. To provide this important function of lateral springing strong vertical arms are mounted in rubber cushions on the truck towers, and are connected to the body by horizontal tie rods running between the coil springs. Space for these parts of the suspension system is provided in pockets in the body, but the space thus taken is small when compared with the enormous benefits made possible. Fore and aft positioning of the truck relative to the car body is maintained by a tubular link mounted below the floor level and attached to the body at one end and the other end to the truck by means of rubber buffers. These rubber buffers cushion longitudinal shocks and permit angular movement of the link when the truck moves relative to the body, thereby making it effectively a pin-ended strut which transmits braking forces and other longitudinal loads between truck and body.

Both vertical and lateral motions of the car body relative to the truck are controlled by hydraulic shock ab-

Fig. 3—Articulate Truck and Suspension Structure



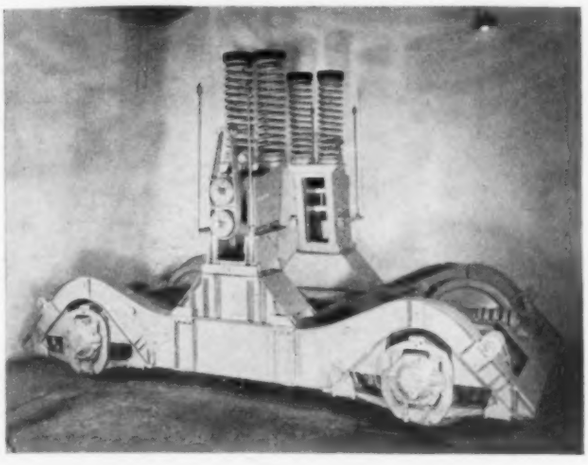


Fig. 4—End Truck and Suspension Structure

sorbers, to prevent building up harmonic motions. The vertical springs have a static deflection of about 9 inches, giving a frequency of approximately 65 cycles per minute, which is considerably better than most modern automobiles, and far better than anything the railroads can offer at present. The lateral springing is adjusted so that the normal lateral rate is about 36 cycles per minute, and hard lateral shocks are cushioned by rubber instead of resulting in sharp metallic impacts as is the case in most standard railroad trucks. A very important feature of the lateral springing is that it is located above the center of gravity of the body so that lateral forces, such as centrifugal force on curves, cause the body to roll or bank in the correct direction as a stable body, rather than rolling outward as do present railroad cars, as illustrated by Fig. 2. The very soft vertical and lateral springing of this suspension system, together with the complete elimination of incorrect roll, has made possible a railroad car which has proved itself to be much steadier and smoother riding than the best type of modern railroad car.

The suspension system just described is far simpler than that of present standard trucks. It incorporates no working joints carrying body loads, nor are there any points where slack or wear can develop. The coil springs and lateral control arms of the new truck replace swing hangers, spring plank, bolster springs, bolster, bolster wear plates, center plate and side bearings in the usual truck construction.

The trucks themselves, upon which the suspension structure just described is mounted, are designed for light weight and durability. In the experimental two car unit there are three trucks, two full end trucks and a separable articulate truck joining the two bodies (see Figs. 3 and 4). The end trucks have a nine-foot wheelbase. A twelve-foot wheelbase of the articulate truck is possible because the load from the body is carried directly over each axle and thus the frame is subjected to no bending moments due to body load. The articulate truck frame has been made separable so that cars articulated at both ends could be uncoupled and switched to provide flexibility in train make-up. The truck frames were fabricated from high strength steel arc welded and stress relieved, and will be discussed in more detail later. All wheels have a cylindrical tread and are 30 inches in diameter: they are roll-forged in a single piece of alloy steel and machined all over. Axles are made of heat-treated high-carbon steel, hollow bored and machined all over. Ball bearing journals are used. A standard clasp brake system is employed. On the full trucks one air cylinder operates the shoes on all four wheels through a simple, fully equalized linkage; each half of the articulate truck has its own brake system with an air cylinder operating the shoes on two wheels. A very simple method of automatic slack adjustment is provided.

Protection of the truck frame, bearings, wheels and axles from high impact loads is provided by vertical and lateral cushioning at the journal box. In the truck photograph (Fig. 4) it can be seen that the load is carried from the frame into the ends of the leaf journal spring, the center of which seats in a hanger surrounding the journal box. Vulcanized rubber pads at the top and sides of the journal box, by slight shearing deflection, allow the hanger to shift laterally and provide cushioning against side shock loads. Since, in service there are no moving metallic contacts or sliding surfaces where wear or play can develop, the wheels, axles, bearings, and frame are relieved of all shock and impact loads. Brake thrust, but not torque, is carried by the leaf-spring which was designed for this duty. The total weight of each 9-foot end truck is 8800 pounds and the 12-foot articulate truck weighs 10,900 pounds.

To assist in achieving the objective of light weight the body structure is also a radical departure from conventional practice. The experience of modern aircraft manufacturers in developing structures of very high strength-weight ratios has been used in the design of the true "stressed-skin" or semi-monocoque body structure of these new cars. The characteristic feature of this type of structure is that the outer covering of "skin," is used as a principal load-carrying element, suitably stiffened and supported. By proper design the skin is able to carry its load without the usual buckles, or "load-wrinkles."

The effectiveness of this type of structure is remarkably demonstrated by the experimental car bodies which are built of Douglas Fir plywood, the outside shell being $^3/_8$ inch thick (see Fig. 5). A static load test to twice the normal gross load has proved the strength and

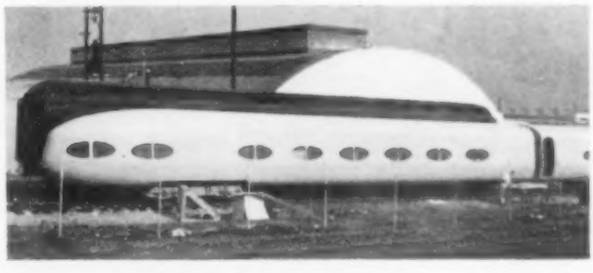


Fig. 5—Rear Car of Hill Experimental Unit Shown Alongside Standard Railway Car

rigidity of these car bodies, which are also entirely free of the creaks and groans usually associated with wooden structures. Wood was used in the test cars merely as a convenient expedient in arriving at suitable means to test the trucks and suspension structure within a reasonable time and at a reasonable cost, and there is no intention of using it as a structural material for future cars. Monocoque construction is admirably suited to a variety of materials, and its application to a proposed steel structure will be described in a subsequent portion of this paper.

To study the action of the rather radical suspension system, as well as to demonstrate the possibilities of the stressed-skin body structure, the two car test unit was built consisting of the wooden bodies and steel trucks just described. Photographs and casual outside views convey the erroneous impression that the cars are small; however, the maximum inside width of 9 feet 9 inches is several inches greater than many standard cars. In spite of a low overall height of 11 feet, an adequate inside height of 8 feet $2^1/_2$ inches is made possible by a 30-inch floor level. The truck suspension structure is housed by well-insulated pockets within the body. The overall length of the front car is 70 feet and that of the rear car is 79 feet. Since the gross weight of the two cars together is only 65,000 pounds, wheel loads are very low.

The front end of the front car is fitted with a special draw-bar to attack to a standard six-wheeled-truck club car. A raised platform in the front test car allows easy passage into the standard car. The tail end of the rear car has an overhang of 19 feet from the center of the rear truck. The center of gravity of the new cars is approximately 45 inches above the rails.

A series of test runs on several conditions of track and at a variety of speeds have proved that the truck and suspension system is fundamentally correct. Preliminary runs made on a local freight line, in relatively poor condition, at speeds up to sixty miles per hour demonstrated conclusively that the light-weight test cars had riding qualities superior to those of the six-wheel truck club car weighing six times as much as either of the test cars. This car was selected for comparison because it was considered a good riding car. In the new cars standing and walking about is accomplished with greater ease and with a more secure feeling than in the standard car, and writing in the new cars is definitely easier. There is also

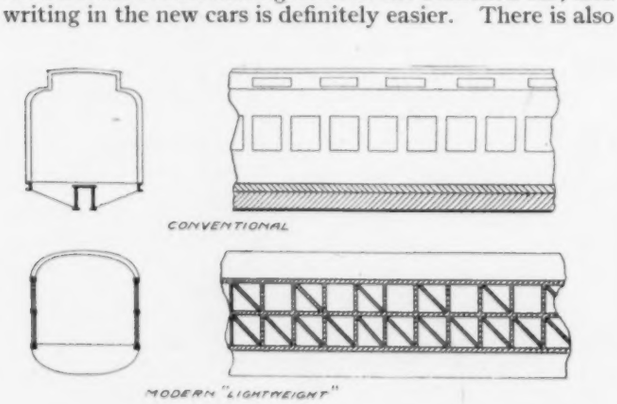


Fig. 6—Typical Methods of Car Construction Showing Principal Load-Carrying Structure

a pronounced lack of body roll, and sudden side movements of the truck, caused by sharp lateral irregularities of the track, are definitely cushioned. The results of these tests were so satisfactory the Santa Fe Railway decided to run high-speed tests on their mainline tracks. For this purpose a special test train was made up, consisting of both units of the 3600-hp. Super-Chief Diesel locomotive, the Santa Fe's dynamometer test car, a new "light weight" stainless steel coach, the standard club car, and the two experimental cars. A train of truly unusual appearance, and well worthy of the title "Screwball Special" which it soon acquired. The first run was made over Cajon Pass to Barstow and return, via San Bernardino. The top speed reached was 94 miles per hour. Ride instruments and personal observations conclusively proved that at all speeds the new cars were definitely smoother riding than either the old heavyweight or the new light-weight standard cars. Vertical and lateral accelerations were appreciably lower, there was a complete lack of incorrect body roll, and practically no vibration was felt. One of the outstanding features of the new type of suspension is the complete accessibility and visibility of all working parts of the truck. Shock absorbers have actually been changed while travelling at high speeds, and the lateral ride can be varied at will by very simple adjustments "en transit." Through convenient windows it is possible to watch the functions of the journal box elastic system, as well as the action of the coil springs and lateral control arms absorbing the "crazy dance" of the truck as it faithfully follows every irregularity of the rails.

To check the observations made on this run, as well as to study the effects of some minor changes a second run was made to San Bernardino and return, with the same train except for the substitution of a steam locomotive. The top speed on this run was 80 miles per hour. Because of the changes even better riding qualities were obtained on this trip. One of the most important things demonstrated by these several test runs was that such a simple mechanical system, designed according to sound fundamentals of mechanics, will behave exactly as it is supposed to, and that logical changes will give logical results. There can be no question that such a simple system, fundamentally sound, is better and more economical than the more complex system now used to serve a similar purpose. It is, therefore, considered reasonable to expect that the simple truck and suspension system used on these new cars may eventually replace the more complicated trucks now in general use.

This same reasoning applies to the body structure of the new cars. Here three fundamental objectives have governed the design: light weight, high strength and low cost. The first two, light weight and high strength suggested the application of the type of structure so successfully used in practically all modern airplanes, namely stressed-skin, or semi-monocoque structure. This type of structure, as its name implies, derives the principal part of its strength from the outside covering, or "skin," of the body. This method of construction saves considerable weight by virtue of the elimination of a certain amount of the usual framing and truss work of conventional body structures, allowing the over-present outer covering to do the work instead. This is diagrammatically illustrated by Fig. 6 which compares the principal load carrying structure of two types of standard cars with that of the new cars. The old conventional structure consisted primarily of a heavy center sill below the floor, sometimes assisted by smaller side sills, which literally formed the backbone of the car, supporting the weight of all of the rest of the structure. Obviously this type of structure is inefficient and heavy. A great

Fig. 7—Interior View Showing Structure of Hill Experimental Car Body

step forward was made by the introduction of the side wall truss type of construction, in which the center sill is of secondary importance as a vertical load carrying member, and the side walls of the car are built as deep trusses. This results in a much more efficient structure, and appreciable weight savings over the older cars have been made. The new stressed-skin cars are still another step forward. Here the center sill serves only to carry end loads and the complicated side trusses have been completely eliminated, the always necessary outer covering being especially designed and reinforced to carry all the primary loads.

Essentially, then, the stressed-skin body is a huge tubular beam the full depth of the car body. Under normal loads the roof acts as the compression flange, the bottom as the tension flange, and the two sides as the shear webs. Figure 7 is an interior view of one of the wooden bodies, the structure being similar to that which will be used in the metal bodies.

Theoretically, if the skin were heavy enough to behave itself it would be all the structure necessary. However, our deep beam is so effective that only a very thin skin is needed (about 16 ga.) and hence it must be adequately supported by transverse ribs to maintain the shape of the body, and on the compression side (roof) it must be reinforced with longitudinal stiffeners to assist in carrying compression loads. Bulkheads enclosing the suspension structure at each end of the body transfer the load reactions by shear to the side sheets. These bulkheads also serve to make the entire body a torsionally rigid tube.

Elastically this structure acts, with certain allowances, just as does any other beam, and the section properties and resultant stresses are a matter of straightforward calculation using conventional beam formulas. At a certain calculable stress the unstiffened skin under compression buckles and no longer continues to assume increased loads. However, the longitudinal stiffeners together with a portion of the skin adjacent to them continue to carry increased loads, acting as columns supported laterally at intervals by the ribs. The rib spacing, therefore, determines the column properties of the skin-stiffener combination, and is usually made such that these parts are in the short column range. The effective width of skin acting with each stiffener is a function of the stiffener stress, the thickness of the sheet, the stiffener spacing and the modulus of elasticity of the material.

Simplicity of design calculations is obtained by analytical procedure enabling use of the standard beam formulas in determining structural stresses. Experience has shown that this procedure gives results within the limits of accuracy required. In calculating the section properties the full area of the skin on the tension side may be used but on the compression side only that portion can be included which is considered to be acting with the stiffeners at their full stress, as previously explained.

For psychological reasons it is desirable that the skin should have no buckles under normal loads, and this requirement governs the allowable compressive stress for the normal loading conditions, thereby determining the thickness of the roof sheets. The ultimate design factor determines the ultimate load to be carried and this governs the number and size of stiffeners for the roof sheets.

It is desirable to proportion the body section so that its neutral axis is as near the compression side as possible, resulting in lower stress in the roof, since the allowable compressive stress of the roof structure is naturally less than the ultimate tensile strength of the bottom sheets. This means that heavy sections below the neutral axis should be avoided.

Normally the shearing stresses in the side skin are relatively low for two reasons; shear loads are divided between the two shear webs, and the effective web depth is large. However, the necessity for windows reduces the shear area, and the window spacing is necessarily somewhat limited, but not to an undesirable extent. To assist in improving the shear conditions around the windows they have been made elliptic in shape, thereby disposing of sharp corners and eliminating stress concentrations. These elliptic windows also blend well with the general appearance of the cars.

The structure described so far has as its primary function the carrying of vertical loads resulting from its own weight and the weight of the equipment, furnishings, and live load. However, the car body structure must also be capable of protecting the passengers in the case of unusual loads such as may occur in collisions or other accidents. To meet railroad requirements for end collision strength a center sill and side sills are being provided below the floor. These sills will add their column strength to that of the body sheet which is reinforced at the ends by rigid frames resulting in a structure extremely capable of resisting end collision. Resistance to side collision is provided by the side sills and the many transverse ribs.

Preliminary designs are in progress for a production car body. A study of available materials has indicated that the new high-tensile steels are particularly well adapted to the structural requirements as they combine high strength with corrosion resistance making practical the use of light gages. They are also readily joined by several of the already well developed welding methods, and last, but not least, are relatively inexpensive. Welding, both electric-arc and spot, has been accepted without qualification as the most desirable method of joining the various members of the structure. With this in mind the structure is being designed to make possible the most effective use of welding, and many opportunities are open for the use of automatic equipment. In fact, it is expected that the ratio of automatic welding to hand welding will be higher with this type of structure than with any other. There is no multiplicity of short members interconnected with gusset plates. Practically all joints are long continuous seams and there are no points of load concentration where welded connections require exceptionally high strength.

Figure 8 is a side view of the proposed steel car body. The following table gives some of the pertinent design data.

Design weights 65 passenger day coach

Body Weights		Total Car weights	
Body structure	17,000	} Car body-empty	40,000
Equipment & Furnishings	23,000	} Trucks	17,000
Passengers & Baggage	16,000		
		Car Weight-Light	57,000
Body Design Weight	56,000	Passenger & Baggage	16,000
		Car Weight-Loaded	73,000

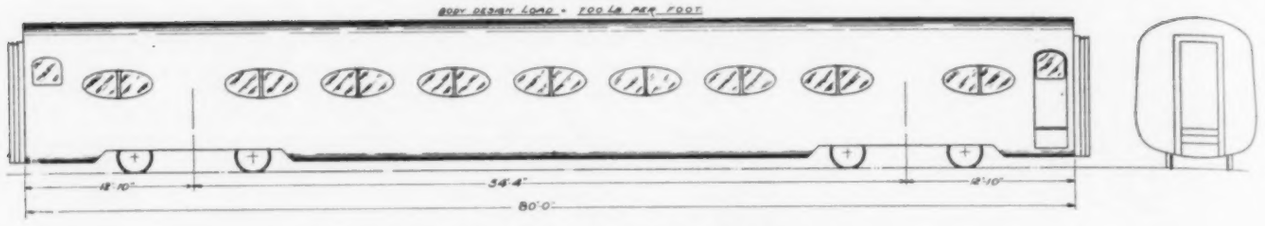


Fig. 8—Proposed Steel Day Coach with Estimated Weights

Total weight of car (empty)		57,000 lb.
Total weight (with passengers and baggage)		73,000 lb.
Truck weight	17,000 lb.	
Body weight	17,000 lb.	
Equipment & Furnishings	23,000 lb.	
Passengers, Baggage, etc.	16,000 lb.	
Total design body load		56,000 lb.
Length of car, overall		80 feet 0 inches
Distance between truck centers		54 feet 4 inches

Figure 9 is a typical cross section of the body, showing all the structural members, and Fig. 10 illustrates a number of the welding details.

The principal body structure consists of four subassemblies; the roof structure, two side panels, and the bottom and floor structure. The roof structure consists of two side channels formed of 0.125 sheet running the length of the car, the 18 ga. transverse ribs which are notched to receive the 16 ga. hat-section continuous longitudinal stiffeners, and the 16 ga. cover sheet. The side panels are of similar construction and consist of a continuous upper side channel formed of 0.125-inch sheet, a lower side channel of 1/4-inch plate, the transverse ribs, and the 16 ga. side sheets. There are no longitudinal stiffeners, but the side sheets have window openings and window framing must be provided. The bottom assembly consists of the two longitudinal side channels formed of 1/4-inch plate, the rib segments, the floor truss built up of 16 and 18 ga. sections, and the center sill. The floor trusses transfer the service loads to the ribs and thence to the side sheets, and also serve to

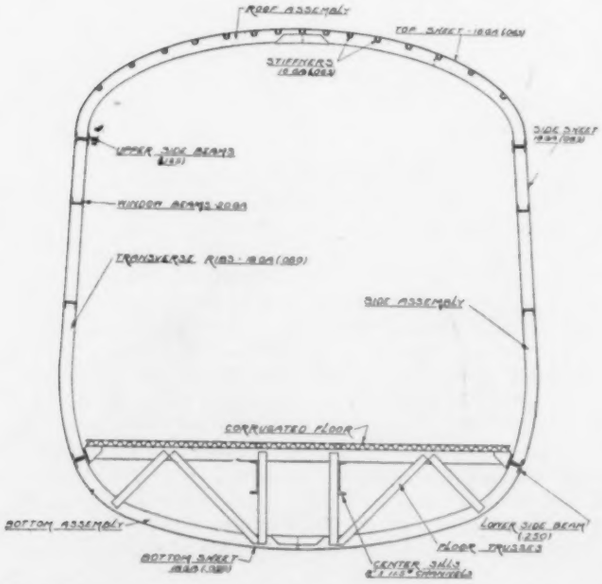
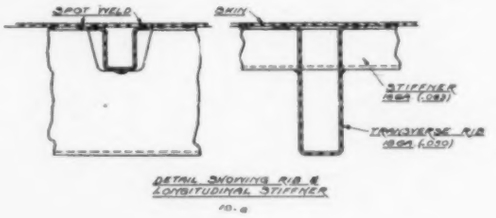
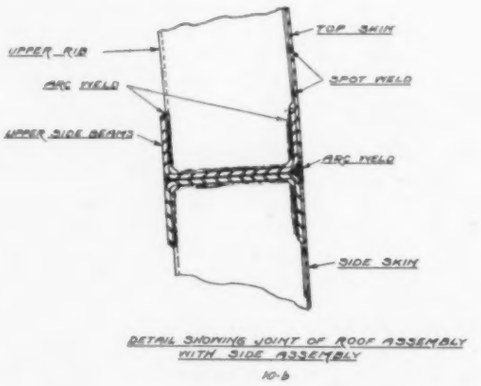


Fig. 9—Proposed Structure for Steel "Stressed-Skin" Railroad Car Body





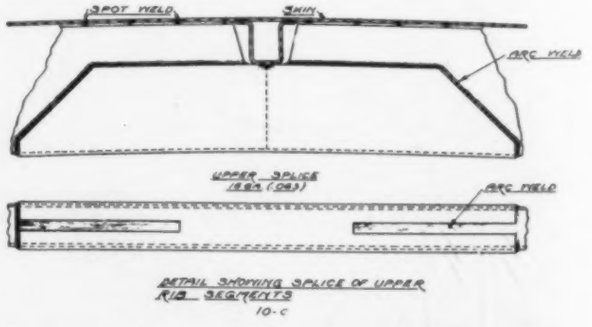


Fig. 10—Typical Details of Body Construction Showing Possible Applications of Welding

stabilize the draft sill, making it extremely effective as a column for heavy end loads.

It is proposed to attach longitudinal stiffeners to the upper skin by spot welding (Refer to Fig. 10*a*). The seams are straight and continuous the length of the car, making possible the consideration of some method of automatic spot-welding. These welds are required to carry a small amount of horizontal shear due to the transfer of load from the skin to the stiffener. However, under normal load this amounts to a maximum of only 3.5 pounds per inch per stiffener, so the spots are required to transfer very little load. Similarly the skin may be attached to the transverse ribs by continuous seams of spot welds. Here again the load transfer is normally small, as the principal function of the ribs is to act as secondary lateral support for the stiffeners and skin which are carrying compression. However, the ribs are subjected to a small amount of bending in maintaining the shape of the structure, and in case of side collision may receive fairly high bending loads. The skin acts as the outer flange of the rib, and also serves as a splice across the stiffener cutouts in the roof segments, hence it is desirable to have a reasonably good shear connection from skin to rib. The top and bottom ribs will be made in two parts, spliced as shown in Fig. 10*c*. The channels which form the side beams act as caps for each of the sub-assemblies, and serve to stiffen and support these assemblies during fabrication. The ribs nest into them as indicated in Fig. 10*b* and will be attached by arc-welding.

The final assembly will be made by bringing the four completed sub-assemblies together and joining them by arc welding the channels together as shown in Fig. 10*b*. This operation will involve four continuous fillet-weld seams on the outside, and four seams on the inside which may be either intermittent or continuous. These welds serve not only to join the sub-assemblies, but the outside seams tie the cover sheets together and transmit the horizontal shear loads. Here again the shear loads are low, the maximum being only 125 pounds per inch under normal loading conditions. These welds also act to splice the rib segments together and transmit transverse bending loads from one rib segment to the other. Because these welds are straight and continuous for the entire length of the car the use of automatic equipment may be feasible. To facilitate automatic arc welding a positioning jig can be used which will permit the body to rotate about a longitudinal axis and thereby enable all welds to be made in a horizontal position.

The fabrication of the principal load-carrying structure has been explained and it is seen that the welding problems are comparatively simple. The parts are all of relatively light gage and can be designed for the most effective use of spot and arc welding. The important welded joints are continuous and straight, making feasible the consideration of automatic equipment. By making the body structure in four complete sub-assemblies the jigs can be simple and the parts will be easier to handle. Each sub-assembly becomes a complete unit, and the final assembly reduces to a simple welding operation.

It is still too early in the design stages to discuss many of the important details such as suspension bulkheads, vestibule, structure end bulkheads, steps, floor details, center sill and other parts of the body. However, these parts do not involve any particularly unique features, as does the main body structure, hence their discussion would not be of especial interest. Suffice it to say that most of these parts will involve light-gage sheets, and will be designed for fabrication by welding.

Summarizing it is seen that a car body structure specifically designed to make use of the ever-present outer covering can be fundamentally simpler than other structures designed for the same purpose. The number of component parts is reduced to a practical minimum, and because of the effective design almost every part is

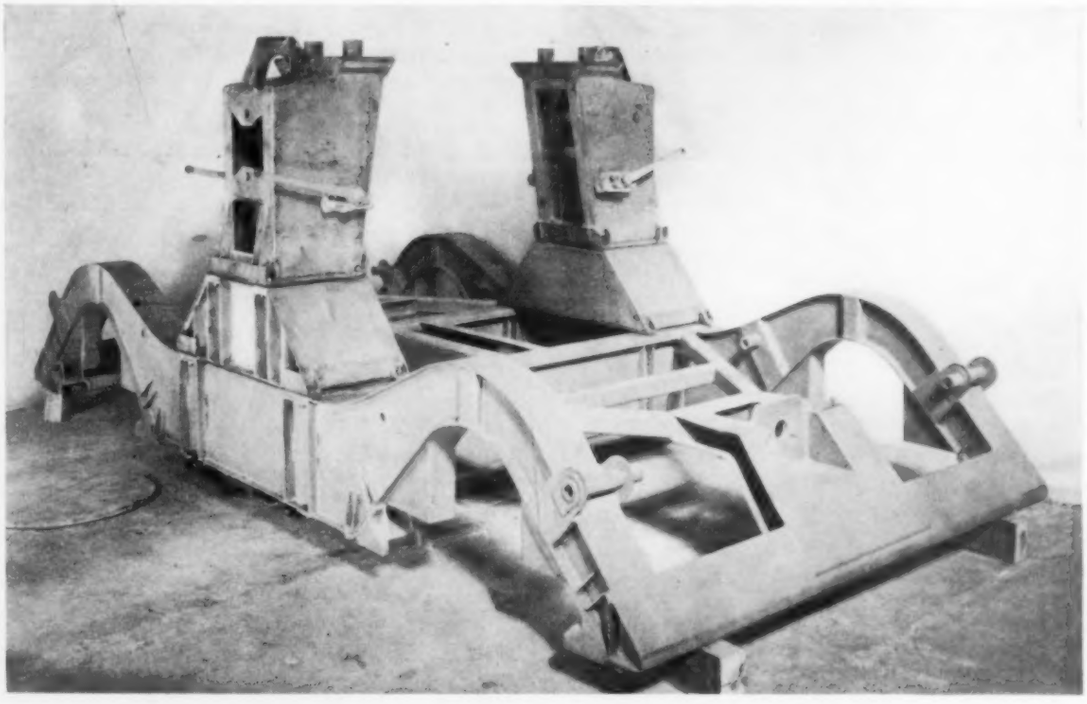


Fig. 11—End Truck Frame and Suspension Towers Welded of High-Tensile Steel

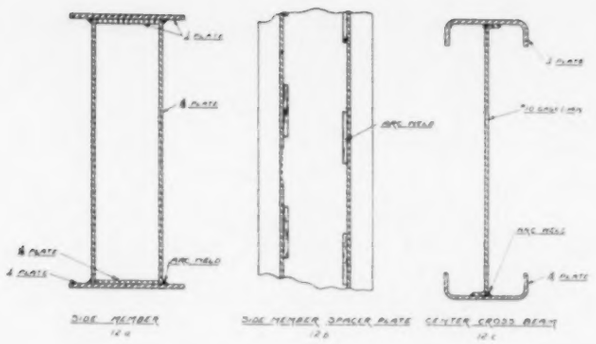


Fig. 12—Details of Truck Frames Showing Welding Applications

carrying its share of the load, making it possible to use light sections. Because each part is relatively lightly loaded and there are no points of high load concentration the problem of joining these parts is not difficult. An estimate of the amount of welding in the principal body structure of an 80-foot car indicates that there will be approximately 9000 lineal feet of welding, both spot and arc. Of this 7000 feet will be continuous straight line spot welding and 700 feet will be continuous straight line arc welding. There remains an equivalent of 600 feet of miscellaneous spot welding and 700 feet of arc welding, principally tacking and miscellaneous short welds. The majority of the spot welding will involve 16 and 18 gage sheets, while the arc welding will involve 16 and 18 ga., $^1/_8$-inch and $^1/_4$-inch materials.

As was previously explained this description of the metal car body structure is the story of a structure which has not yet been built, but which is still on the designing tables. The purpose of this description has been to present this new type of railroad car structure and the interesting possibilities it offers for welding. The methods of fabrication described are purely tentative and are subject to change as the result of experience and suggestions.

Although the steel car body is still in the design stages three trucks have been built for the experimental cars and have successfully passed severe running tests. The frames for these trucks have been fabricated of one of the high-strength steels, arc welded and stress relieved. Two types of trucks were built—one articulate truck joining the two cars, and two end trucks. The welding problems were the same for both types and since articulate trucks probably will not be used on future cars any description of construction details will refer to the unit end trucks.

One of the unit truck frames is pictured in Fig. 11. It consists of the main frame, two tower bases and two towers. These towers extend into the car body and carry the suspension coils, lateral restraint arms and shock absorbers, some of which are shown in position. The towers and tower bases are attached to the main frame by bolts to make it possible to remove the trucks without lifting the body over the towers.

The main frame consists essentially of two side beams which distribute the loads to the journal boxes and wheels together with transverse beams and diagonals. The side beams are of box section as shown in Fig. 12*a*. The side sheets or webs are $^3/_{16}$-inch sheet torch-cut to shape. The cap strips are of $^1/_4$-inch plate, bent to the contour of the side sheets. Additional area is obtained by welding a $^1/_4$-inch spacer strip to the inside of the cap strips. These spacers are used as back up strips for welding the web to the cap strips and in order to avoid interference from the weld fillets the spacers were notched as illustrated in Fig. 12*b* and welded to the cap strips by filling in the notches. The webs and caps are welded together with continuous fillet welds. The flanges and webs were tack welded together prior to welding. To minimize distortion the welding was done in short lengths moving from one part of the work to another and allowing each portion to cool before adjacent weld was added. The transom beams which resist side bending loads are built up in the form of deep I beams as illustrated in Fig. 12*c*. The webs are 10 ga. sheet reinforced with vertical stiffeners through the sections of high shear load. The upper, or compression, flange is a channel formed of $^1/_4$-inch plate, and the lower flange is a similar channel of $^3/_{16}$-inch sheet. The web was flanged along each edge to assist in maintaining straightness and rigidity during welding. The end beams are formed from a single piece of $^1/_4$-inch plate, welded along the joining edge, as shown in Fig. 12*d*. The draft fittings at the ends of the truck are built up of $^1/_4$-inch plate with a $^3/_4$-inch plate to transfer the draft and buff loads from the draft tube to the fitting. The loads are transferred from these fittings to the truck frame by means of diagonals formed into a C section as shown in Fig. 12*c*. Each sub-assembly was built up separately and then the whole assembly tack welded together prior to final welding. Final assembly welding was then completed with the usual precautions to minimize distortion by avoiding excessive heat concentrations. After the frames were completed they were checked and found to be remarkably true.

The tower bases and towers were built up of $^3/_{16}$-inch and $^1/_4$-inch high-tensile plate, formed to shape and arc welded.

Inasmuch as these frames were to be used under experimental cars which would be carrying passengers and subjected to severe road tests it was decided to exercise the additional precaution of stress relieving. The frames were carefully supported on the furnace bed at many points so none of the members could sag. The furnace heat was slowly increased over a period of four hours to 1160° F., held at this temperature for one and a half hours, and then slowly decreased. A careful check after stress relieving showed that there was no distortion.

Before any welding was done a series of tests were conducted to choose between two recommended welding rods; one was designed specifically for use with the high-tensile steels, and will be referred to as Rod *A;* and the other was one commonly used with mild steels, and will be called Rod *B*. Several types of specimens were made enabling tests to be made of the shear and tensile strength of the metal adjacent to the weld, and permitting visual inspection of the weld structure. The effect of stress relieving on the physical properties was also studied. The specimens were of the same gage as the frame material, and the test welds were single-pass welds, as were used on the frames.

In the as-welded condition the grain structure of both the *A* and *B* weld metals was rather coarse. Stress relieving had an appreciable refining effect on the *B* metal, but no apparent effect on the *A*.

The tests indicated the following properties in the weld metals:

Shear	As-Welded	Stress-Relieved
A	45,000 lb./in.²	45,000 lb./in.²
B	40,000 lb./in.²	39,500 lb./in.²
Tensile		
A	68,500 lb./in.²	67,000 lb./in.²
B	66,000 lb./in.²	67,300 lb./in.²

It is apparent that stress relieving had little effect on the shearing properties of the weld-metal. However, the tensile properties do show some effects from stress relieving. In the as-welded condition *A* was slightly stronger than *B*, but stress relieving apparently lowered the strength of *A* and increased the strength of *B* so there was little difference between them.

The tests indicated that the strength of the base metal adjacent to the weld was not adversely affected by welding, but stress relieving did reduce the physical properties slightly. It was found that the *B* weld shows definite improvement of grain structure as a result of stress relieving, whereas the *A* weld did not show any marked improvement in grain structure.

Although the tests were not sufficiently comprehensive to make definite conclusions possible they do indicate that in the sizes and types of welds used there is very little difference in the strength of the two weld metals tested.

The lack of any appreciable difference between the two weld metals may be accounted for by the fact that they were single-pass welds, and that in depositing the single layer there is of course a certain amount of mixture of the weld rod metal with the base metal. This mixture may result in a weld material whose properties are largely influenced by the base metal, and which will show no pronounced difference between the two types of rod used. It is possible that a greater difference between the *A* and the *B* welds would be noted in multiple-pass welds because the additional weld layers would consist of nearly pure weld rod material, with less influx of the base metal.

That the quality of materials and workmanship and the methods of construction have been excellent is attested by the fact that there has never been the least sign of a weakness in the truck frames after the rather severe road tests to which they have been subjected.

The experience gained from the trucks of the experimental cars has proved that welding is an effective and reliable method of fabrication for light-weight truck frames, and it is confidently expected that it will prove itself admirably suited for the fabrication of the radically new stress-skin, light-weight car bodies. This new type of fabrication combined with new and simplified methods of welding procedure is expected to develop economies in car construction which may result in expanding another field of the welding industry.

HOW TO REBUILD CUTTING EDGE OF WORN Dipper Teeth with Welding

By F. J. HIRNER†

HERE is a simple and low-cost way of building up worn dipper lips and teeth right in the field—saving the expense of buying new parts and cutting down the time lost to an absolute minimum.

Practically all power shovel and dragline buckets are equipped with lips and teeth made out of some type of manganese wear-resisting steels. Thus, in building up manganese steel sections, it is advisable to use a coated electrode having approximately the following chemical analysis:

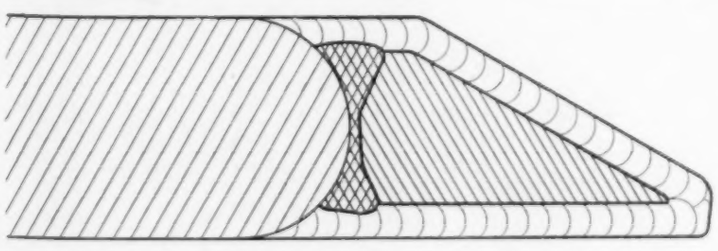
Manganese.....11 to 14%
Nickel.....3½ to 4½%
Carbon.....Approximately 1%

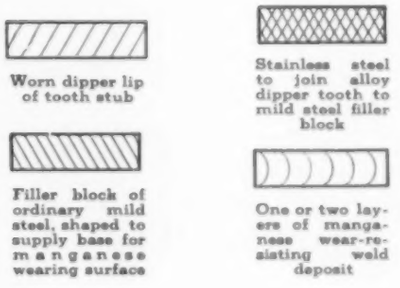
This alloy has distinct physical properties which render it particularly valuable for use on operations imposing combined abrasive wear and excessive impact or shock. The light coating used on this rod stabilizes the arc and protects the metal against loss of carbon and manganese. However, the slag volume is kept at a minimum so as not to interfere with rapid cooling which is essential to the formation of an austenitic deposit.

In the "as-welded" state this high manganese deposit is soft and ductile, but cold working such as hammering,

† Harnischfeger Corporation.

How to Rebuild Worn Out Dipper Teeth with Welding




Worn dipper lip of tooth stub
Filler block of ordinary mild steel, shaped to supply base for manganese wearing surface
Stainless steel to join alloy dipper tooth to mild steel filler block
One or two layers of manganese wear-resisting weld deposit

peening, etc., decomposes the soft austenite to extremely hard martensite, and when once formed, this martensitic layer offers resistance to impact and abrasion.

Welding Procedure:

Polarity—reverse, Electrode—positive, work—negative

Arc Length—

A medium length arc is recommended—24 to 26 volts.

Size	Current Rates, Amperes
1/8 inch	6–100
5/32 inch	90–150
3/16 inch	120–200
1/4 inch	170–260

These welding currents are general and should be adjusted in accordance with the particular application and the type of electrode used. Flatter beads and greater penetration are produced in the high heats, although care should be taken to avoid the use of excessive welding heat.

LAYING THE BEAD

The manganese steel surface to be built up should be divided into areas approximately 1 1/2 inch square, and the amount of metal deposited at one time should be confined to this area. A rather wide bead, applied in a semicircular motion is recommended. But no more than 3/16 inch thickness should be deposited at one time.

Vigorous peening of the hot bead with an air or hand hammer is essential to relieve shrinkage stresses, and to increase surface hardness While the deposit should be hammered smooth, it is important not to do this peening to excess—that is, to a point where the metal starts to flake—indicating that the deposit has reached the point of brittleness.

The diagram above shows how the weld-metal should be applied, and should be studied carefully before work is started. Any operator following this procedure will shortly become expert at a job that formerly was a constant source of annoyance and expense.

FLAME HARDENING AND ITS Principles

By H. H. LANGEBECK†

MUCH has been written in the last years about the various methods of flame hardening and its application to steel and cast-iron products. In the paper presented here an attempt has been made to bring out some basic factors involved in this interesting heat treating process and, by dealing with them separately, to clarify the somewhat complicated mechanism.

Of the four flame hardening methods which have been described in a paper on the same subject by A. K. Seemann in *Metal Progress*, Sept. 1937, two methods really represent procedures basically differing from each other, namely, the progressive method and the spinning method. With the former only a limited portion of the surface is heated by the blowpipe traveling at a rate of about six inches per minute along the work surface. As quenching immediately follows the heating operation the work is thus progressively hardened. With the spinning method the entire surface of the work is gradually heated up to the desired temperature after which quenching all along the circumference of the work is performed in a second operation.

Flame hardening whether done progressively or separately by way of spinning and subsequent quenching shares the two outstanding characteristics of any of the various differential hardening processes, namely, the degree of hardness or simply the hardness in terms of conventional hardness units and the depth of hardness or hardness penetration in terms of inches.

The hardness depends mainly on: the chemical composition of the work material and the rate of cooling as conditioned by the character of the quenching media employed.

The hardness penetration is largely influenced by: the distance of flame tip from surface, the tip size and hence the flame volume, the traveling speed of the flame relative to the work or vice versa and the wall size of the work.

† Metallurgical Engineer, Pittsburgh.

In order to elucidate the effect of the chemical composition one single grade of steel in which C and Mn are the only variables shall be considered. This grade is being used for the manufacture of certain oil country goods. It is a fine grained air hardening steel with Mn around 2.50% and C below 0.33%, and has been developed by several steel makers in this country for the particular purpose to combine high fatigue strength with great wear resistance. From a compilation of various steel analyses for flame hardening which is given in the *Metals Handbook* 1936, p. 635, it appears that the aforesaid steel grade represents the two extremes with C at the very low side and Mn exceeding by one per cent the highest amount mentioned in this compilation.

By dealing with one single grade in which the Si, P and S content is fairly constant, the effect of rising Mn and C contents on the flame hardness can be well studied (see Table 1). In Fig. 1 the original hardness and the flame hardness has been plotted against the Mn content. The figure in brackets denotes the C content increasing in almost the same proportion as the Mn content. While the original hardness curve rises but slightly from 210 to 300 Brinell, the curve indicating the flame hardness condition runs much steeper, namely, from 350 to 580 Brinell. Once the flame hardening device has been adjusted for the greatest degree of hardness possible for a given steel analysis the hardening conditions which involve heating and quenching operations can be kept practically constant. For this reason the steep slope in the hardness curve must be accounted for by the rise in the Mn and C content of the various heats employed. Yet even if the hardness conditions are changed between reasonable limits this trend still persists as will be shown farther below.

Anybody familiar with the difficulties in the heat treatment of air hardening steels might anticipate a good deal of surface defects particularly hair cracks. However, very careful pickling tests conducted on a great variety of flame hardened samples showed very definitely that

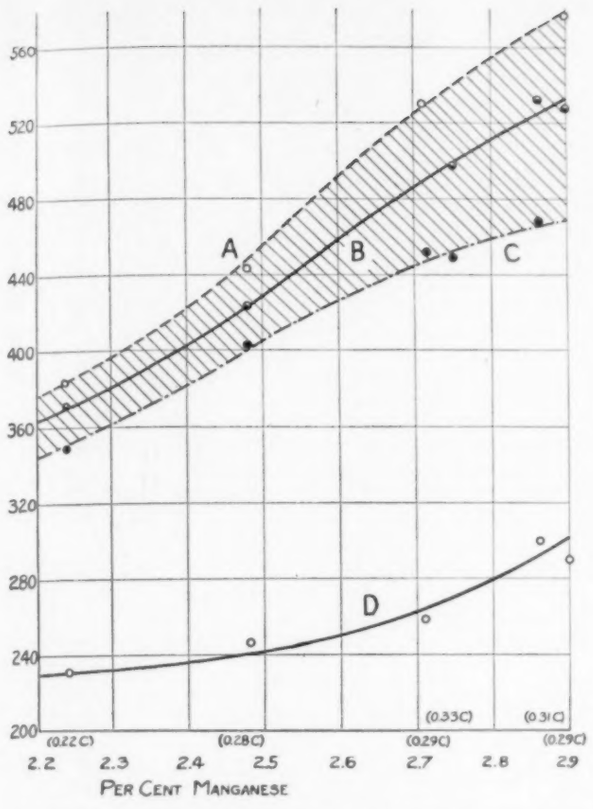


Fig. 1—Original and Flame Hardness in Relation to Manganese Content

Curve A — water quenched
Curve B — compressed air, 15 lb./in.²
Curve C — air draught, 1 to 3 lb./in.²
Curve D — original hardness

Table 1—Original Hardness and Flame Hardness in Relation to Chemical Composition

	Chemical Analysis		Brinnell Hardness*			
Steel	% C	% Mn	Original	Air Draft 1 to 3 Psi	Compr. Air 15 Psi	Water Quenched
1	0.22	2.24	230	348	371	383
2	0.28	2.48	243	402	423	443
3	0.29	2.71	259	457	...	531
4	0.33	2.75	270	451	498	...
5	0.31	2.86	301	468	532	...
6	0.29	2.90	289	...	528	580

* Average of at least four readings.

such cracks never developed even with a Mn content as high as 2.90% provided the flame hardening operations had been kept well under control and the steel to be treated had been properly annealed prior to flame hardening. A good deal of these flame hardened products have stood the crucial test excellently having been in service under most exacting field conditions without developing any surface defects.

What really accounts for the hardness trend in Fig. 1 is the fact that C and Mn belong to those elements which have the capacity of greatly diminishing the critical cooling rate of the steel. According to data published by J. Wellinghous in *Iron Age* 1934, Sept. 13, the pearlitic transformation of a steel with 1.52% Mn and 0.38% C had been entirely suppressed by using a cooling rate of 2000° C. per hour.

Due to the lack of experimental equipment a direct determination of the actual cooling rates employed in the above mentioned flame hardening experiments could not be made. However, based on very exhaustive data afforded by Niels Engel in his book "Untersuchungen über die Stahlhärtung" in which he covers a wide range of cooling rates for liquid and gaseous quenching media, those for the five different quenching media used in our flame hardening experiments have been compiled in Table 2. Engel's cooling rates are related to a 4 mm. Cr-Ni sphere of 1472° F. which according to this authority correspond to those of 40 mm. armco iron sphere of the same temperature. For practical purposes the order in which the cooling rates are arranged in Table 2 might approach the actual working conditions. The respective hardness results obtained from a steel with 0.26% C and 2.55% Mn are entered in column 3.

Table 2—Relative Cooling Rate vs. Hardness

Quenching Medium	Cooling Rate C°/Second	Cooling Rate Relative to Air-1	Brinell Hardness
Still air	40	1	352
Water of 75° C.	95	2.37	429
Compressed air of 15 psi	...	...	444
Oil emulsion 10%	210	5.25	444
Water of 26° C.	1500	37.6	495

In spite of the considerable differences in the cooling rates, particularly in the case of plain water of 26° C. (79° F.) and oil emulsion the increase in hardness, though well pronounced, is comparatively slight. It also appears that water of 75° C. (167° F.) is almost equivalent in its cooling effect to compressed air of 15 psi. Due to the fact that the pearlitic transformation has been everywhere inhibited, the critical cooling rate of the above mentioned steel must have been far below the cooling rate for still air.

With reference to the shaded area in Fig. 1 it might be added that the lower boundary represents hardness values obtained from comparatively slow cooling rates as might happen sometimes due to an accidental drop in the air pressure. The upper boundary line actually reflects the conditions of drastic water quenching. Taking all those factors into proper account a fair prediction as to the hardness which might be expected for a given analysis can be made from a diagram such as presented in Fig. 1.

An important factor which greatly affects the hardness penetration in conventional hardening is the mass effect largely controlled by the chemical composition of the material. Hardening elements such as Cr, Ni, Mn and Mo which strongly diminish the critical cooling rate also have the capacity of rendering the steel more inert to the influence of wall size. For steel grades with 0.26% C and 2.55% Mn a zone of uniform hardness (55 Rockwell "C") was found to extend $^5/_8$ inch below the surface leaving only a small center portion of $^1/_4$ inch with a minimum hardness of 48 Rockwell "C." As long as the heat-affected zone produced by oxyacetylene torch at the work surface does not exceed that critical amount of $^5/_8$ inch the mass effect can be entirely left out of consideration. What really matters in determining the hardness penetration is the heat distribution at and below the surface or to put it more correct the extension of that rim portion whose temperature has been risen above the upper critical of the work material.

Aside from the thermal conductivity which is practically constant for steel grades of similar analysis it is the distance of the flame tip from the work surface, the flame volume, the length of time during which the surface is

Fig. 2—Illustrating Spinning Method of Flame Hardening

exposed to the flame and to a lesser degree the wall size (here not be confused with the above discussed mass effect) which are paramount in controlling the heat penetration and consequently the hardness penetration too.

Very careful investigations on the temperature distribution in the oxyacetylene flame have shown that the maximum temperature prevails at a point located 0.12 inch from the inner cone of the neutral flame (F. Henning and C. Tingwaldt, *Zeitschrift für Physik* 1928, p. 805). A slight excess in acetylene causes a substantial drop in temperature, whereas the flame is much less sensitive to an oxygen increase about twice the acetylene volume. By adjusting the torch for a neutral flame and setting the tip so that the space between the inner cone and the work surface is about $^1/_8$ inch the most suitable distance resulting in maximum surface temperature can be readily obtained for any tip size.

With an increase in tip orifice is connected a proportional increase in flame volume, provided the gas pressure at the reducing valves is properly adjusted. A bigger flame volume means a greater gas consumption per second and, consequently, a larger heat development at the surface which, in turn, will result in a bigger heat penetration. This is shown in Table 3.

If the heat of the flame is allowed to accumulate at the stationary work surface for a long enough time period, melting of the exposed portion will finally ensue. On the other hand, if the traveling speed of the flame is so high that not enough heat can be stored for raising the surface temperature above the upper critical no hardening effect will be noticed. Between those two extremes there are of course all kinds of traveling speeds to cause heat penetrations anywhere from $^1/_{32}$ up to $^1/_2$ inch. An example for that is given in Table 4.

Table 3—Effect of Flame Volume on Hardness Penetration

Size of Tip	Distance of Tip from Surf. Inch	Pressure of Oxygen and Acetyl. in Psi	Gas Consumption Acetyl. in Cu. Ft./Hr.	Gas Consumption Oxygen in Cu. Ft./Hr.	Penetration in Inch
	$^1/_4$	1	3.4	3.5	0.03
33	$^1/_4$	3	5.4	5.5	0.09
6	$^3/_8$	6	10.6	10.8	0.18
10	$^7/_{16}$	10	25.7	25.9	0.30

Traveling speed 4.3 inch per min. Wall thickness $1^1/_8$ inch.

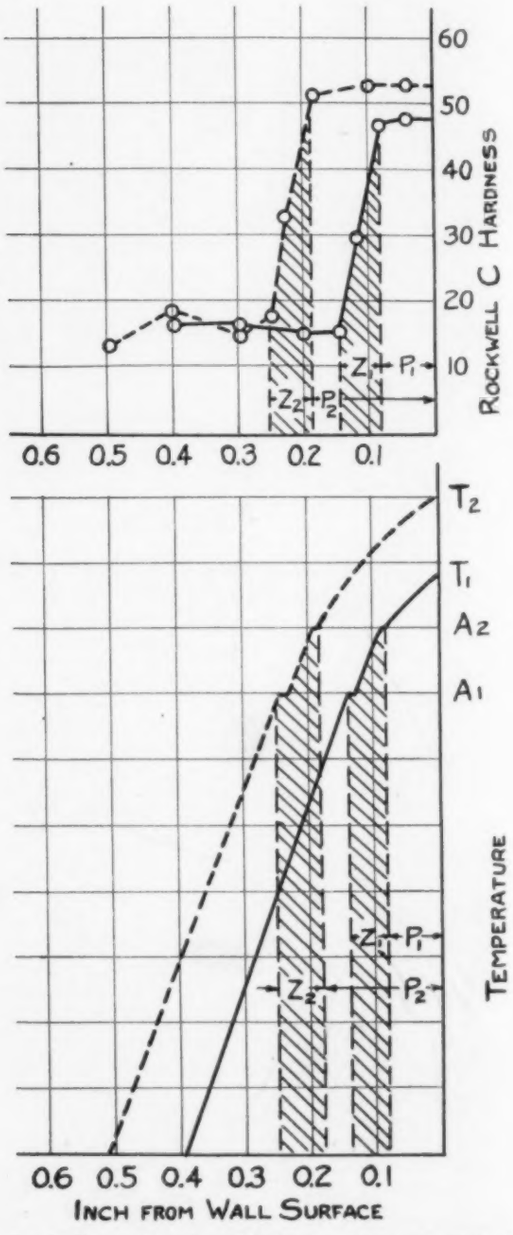


Fig. 3—Proposed Hardness Mechanism (Progressive Method)]
Upper diagram shows hardness distribution
Full line = low surface temperature
Dotted line = high surface temperature
Lower diagram shows assumed temperature distribution curves
Full line = low surface temperature
Dotted line = high surface temperature

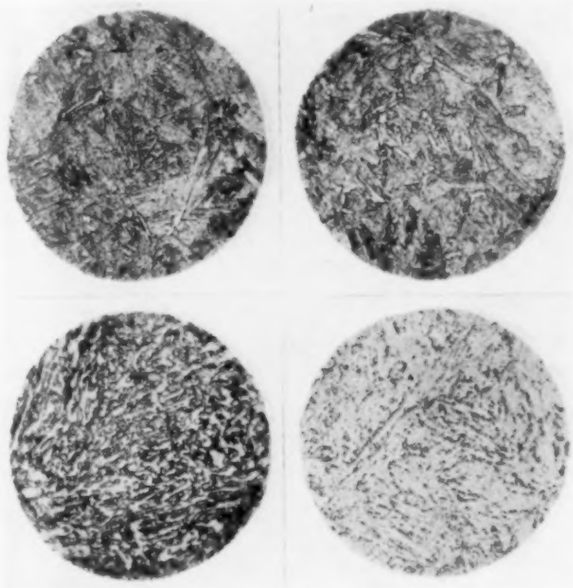


Fig. 4a—Decomposed Austenite with Well Preserved Grain Boundaries 0.04 Inch from Surface (Maximum Surface Temperature) X 1000

Fig. 4b—Decomposed Austenite with Free Ferrite. 0.04 Inch from Surface (Low Surface Temperature) X 1000

Fig. 4c—Transition Zone with Fine Grained Pearlite and Free Ferrite X 1000

Fig. 4d—Original Structure. X 1000

Table 4—Effect of Traveling Speed on Penetration

Traveling speed inch/min.	4.3	5.8	11.5	14.8
Penetration in inch (tip #5)	0.18	0.13	0.04	0.3
Penetration in inch (tip #10)	0.30	0.25	0.10	0.05

As for the spinning method, there is also the relationship between heat penetration and the time period during which the surface is exposed to the flame. This time, naturally, is a function of the total number of revolutions. If, for a given rate of heating, this number is high, the penetration will be deep. On the other hand there must be definite min.mum amount of revolutions to raise the surface temperature above the upper critical. Based on very interesting experimental data published by H. Voss in *Zeitschrift des Vereins Deutscher Ingenieure* 1937, p. 743, the dependence of hardness penetration upon spinning time or total revolutions is shown in Table 5.

The effect of wall size on heat penetration is well known to every gas and arc-welding operator. In order to make up for the large amount of heat dissipated in heavy wall sections he has to apply more heat. The opposite is true of thin wall sections. Now the same principle applies to flame hardening. Consequently, heavy wall sections require slower traveling speed or bigger flame volume or, eventually, both. In a work piece having a reduction in wall size from $1^9/_{32}$ to $^{11}/_{32}$ the corresponding hardness penetration varied from $^1/_8$ to $^3/_{16}$. This difference in penetration was due to the fact that, for practical reasons, both traveling speed and flame volume had to be kept constant.

Since it is the heating mechanism which appears to play a major rôle in both surface hardening processes, the attempt has been made to visualize the heating phenomena involved by means of a scheme such as given in Fig. 3. If the heat coming from the slowly progressing flame is allowed to accumulate at the surface S to such an extent as to raise its temperature T_0 (room temperature) to T_1 that is somewhat above the critical, a part of that heat will penetrate into the inner portions commensurate with the thermal conductivity of the metal. Since the adjacent layers underneath the surface will receive less heat than the surface proper a drop in temperature is the result. Suppose T_1T_0 represents the temperature drop from a point with the surface temperature T_1 to a point farther inside with the room temperature T_0, then the vertical through the upper critical will be the demarcation line between p_1, a zone of fairly uniform hardness and z_1 the transition zone of steadily decreasing hardness as illustrated in the upper part of Fig. 3. Here the hardness found at several locations underneath the surface has been plotted against the distance from the wall. Micrographs taken at a distance about 0.04 inch from the surface (see Fig. 4b) show martensite as the predominant constituent with several scattered areas of ferrite which, apparently due to the lack of time, had not completely dissolved. Fig. 4a shows the microstructure at the same location yet obtained from the highest possible surface temperature as indicated by incipient melting of the

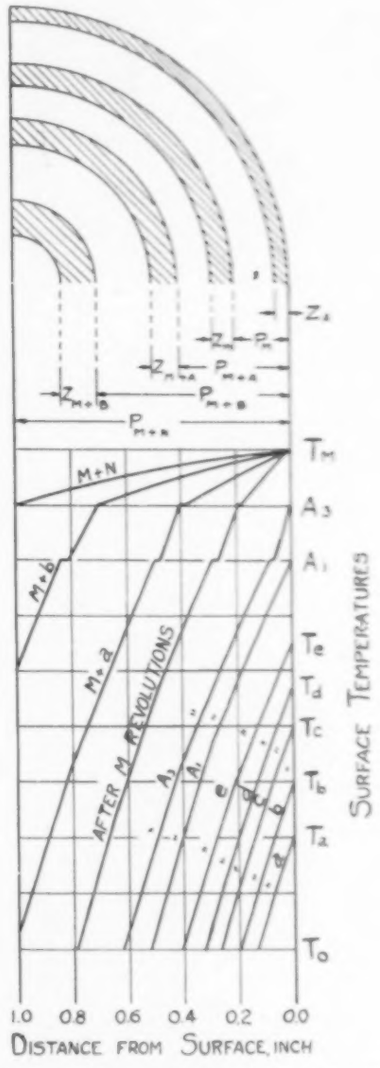


Fig. 5—Proposed Hardness Mechanism (Spinning Method)

Upper diagram shows increase in hardness penetration and growth of transition zone as the number of revolutions is increased.

Z_m—depth of transition zone after M revolutions

P_m—depth of hardness after M revolutions

Lower diagram shows assumed temperature distribution

Curve T_m–T_o is the temperature curve after M revolutions

skin. With a slight increase in hardness, seemingly due to the smaller amount of free ferrite is associated a marked broadening of the hardness penetration as illustrated by the temperature curve T_2T_0. The verticals through the lower and upper criticals, then, form the boundary lines of the transition zones z_1 and z_2 characterized by a steadily decreasing hardness curve with a microstructure as shown in Fig. 4*c*. It is mainly composed of free ferrite and intermediate constituents, i.e., more or less fine grained pearlite.

Based on Voss' investigations on the flame hardening of pearlitic Cr-Mo steel by means of the spinning process a graphical representation of the heating mechanism involved has been tried in Fig. 5. After a definite number of revolutions during which the entire surface is exposed to the flame, enough heat might have accumulated to bring the surface temperature up to the lower critical. At this point the pearlite starts to dissolve which is completed when the surface reaches the upper critical. This process, in contrast to that involved in progressive heating, goes on gradually as with each revolution the surface temperature is continuously increased. By proper adjusting the number of revolutions per min.—equivalent to the traveling speed in the progressive method—the rate of heating can be so controlled as to give the ferrite ample time for becoming completely dissolved. Now suppose the temperature curve T_mT_o in Fig. 5 has been obtained at a certain rate of heating, the zone of uniform hardness would then be pm and the transition zone Z_m. If the time of heat exposure or the total number of revolutions is to be increased, say, to $(m + a)$ revolutions with the surface temperature T_m being held constant at T_m, the rate of heating has to be lowered accordingly. This, however, involves a larger heat dissipation toward the center portions of the shaft and consequently a flattening of the temperature distribution curves. After $(m + n)$ revolutions heat will have been conducted toward the center to such an extent that even there the temperature will reach the upper critical. Quenching performed at that moment will result in a 100% hardness penetration unless the cross section of the piece is so large that the center will not even reach the critical cooling velocity. In Table 5 the steady increase of the penetration zone with

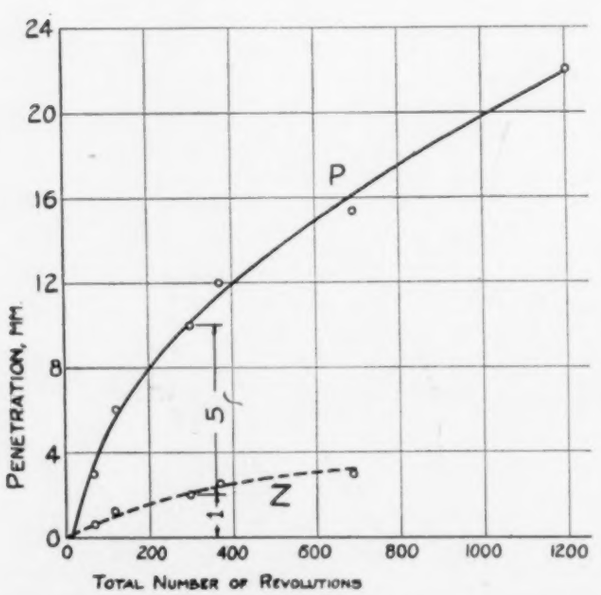


Fig. 6—Effect of Spinning Time on Depth of Hardness and Transition
Curve P — hardness penetration
Curve Z — transition zone

Table 5—Progress of Surface Hardening of 45 Mm. Shaft by the Spinning Method

Sample No.	Penetration Mm.	Penetration %	Transition, Mm.	Spinning Time, Sec.
1	3	13.3	0.6	66
2	6	26.6	1.2	120
3	9	39.9	2	150
4	12	53.2	2.5	372
5	15	66.5	3	690
6	22.5	100	...	1250

increased time of spinning is brought out. With the flattening of the temperature curves in Fig. 5 and the widening of the branches above the upper critical there is also connected a widening of the branches between A_1 and A_3. This, according to the upper part, in Fig. 5 results in increased transition zones. In Fig. 6 the extension of the transition zone and of the hardness penetration has been plotted against the total number of revolutions, using Voss' data. From this it appears that transition and hardness penetration zones are always at a definite ratio, which for the chosen working conditions was one to five.

Whether the same simple mathematical relation exists in the case of progressive hardening has not been verified as yet. Nevertheless, it is the author's opinion that the excellent results achieved by either method are mainly due to the existence of a sizable transition zone. By providing for a ductile bond between the soft base metal and the much more brittle surface layers, after being flame hardened, this transition zone appears to be an efficient safeguard against any trouble due to spalling and chipping.

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Oxyacetylene Welding Stainless Steel Barrels for Transporting Food Products

THE OXYACETYLENE WELDING OF Corrosion-Resistant Steels

By WALTER D. WILKINSON, JR.†

SIMPLE CLASSIFICATION OF HIGH-CHROMIUM STEELS FACILITATES CHOICE OF TECHNIQUE

CORROSION-RESISTANT steels welded properly by the oxyacetylene process will contain welds having physical properties and corrosion-resistant properties equal to those of the parent metal. Although these steels vary widely in composition, properties and uses, they have many welding characteristics in common. It is the purpose of the following discussion to review these fundamental welding procedures, and to describe the special techniques which are employed for welding the different classes of materials.

CORROSION RESISTANCE DEPENDS ON CHROMIUM

The arch enemy of metals is corrosion, which may be defined as the destruction that originates on the surface of a solid body by unintentional chemical or electrochemical attack. Resistance to corrosion in steels is obtained through the presence of suitable amounts of chromium. Corrosion-resistant steels include both the "stainless" type of steels, which are practically immune to oxidational corrosion at ordinary temperatures, and also those steels, which, like the 4 to 6 per cent chromium steels, though not absolutely immune to such corrosion, have several times the resistance—and service life—of ordinary carbon steels.

Although the corrosion resistance of the high-chromium steels is in itself important to industry, the fact that they can be fabricated and installed by welding greatly extends the usefulness of many of them.

GROUPED ACCORDING TO WELDING CHARACTERISTICS

When called upon to weld a high-chromium steel, the operator of oxyacetylene equipment should first find out to which of three general groups the steel in question belongs. Only in this way can he know whether the final welded structure will be suitable for the purpose intended, and, if the steel is suitable, what method he should use to insure satisfactory results. The three groups are as follows:

† The Linde Air Products Company.

1. The straight high-chromium steels that are hardenable (these are air-hardening or martensitic and usually contain up to approximately 14 per cent chromium).
2. The straight high-chromium steels that are not hardenable (these are non-air-hardening or ferritic and usually contain more than 14 per cent chromium).
3. The austenitic chromium-nickel steels (typical of the steels in this group are the well-known 18–8 stainless steels).

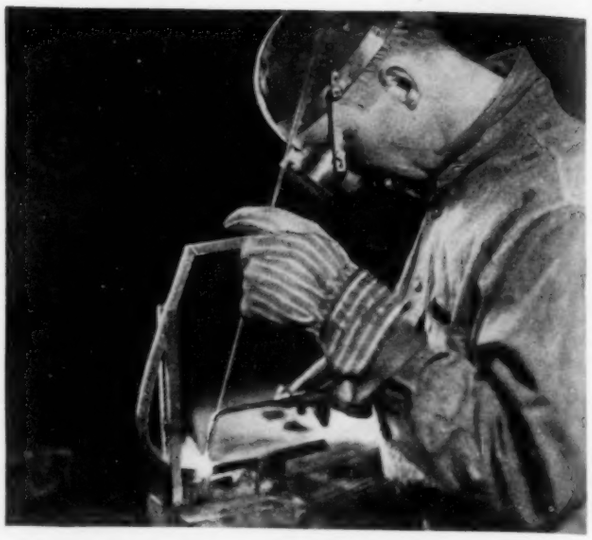
There are many properties, welding characteristics and techniques common to all three types of corrosion-resistant steels. These will be mentioned first, before a more specific discussion of each group.

FLAME ADJUSTMENT

In general, for welding all the high-chromium steels, the welding tip employed should be one or two sizes smaller than that which would be recommended for the same thickness of mild carbon steel.

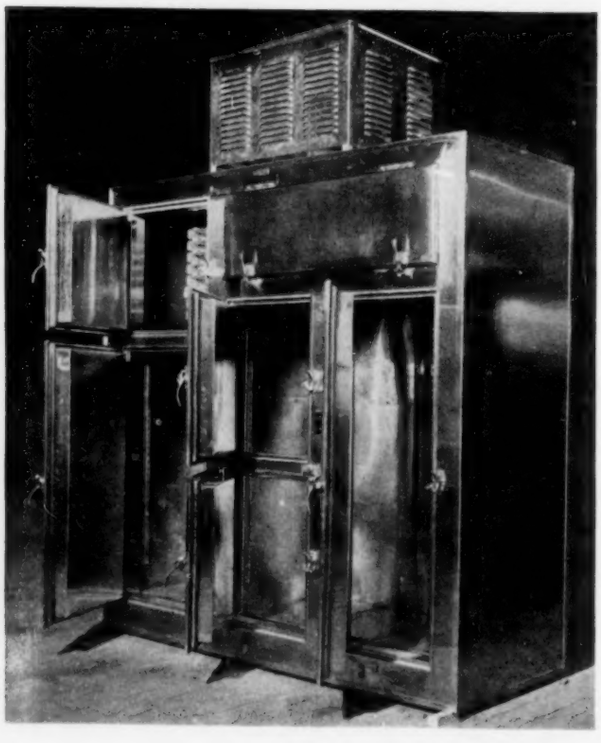
The smaller flame obtainable with the smaller tip is recommended for the simple reason that the heat conductivity of high-chromium steel decreases as the chromium content increases. In other words, since heat tends to remain longer in the weld zones of high-chromium steels, the use of a smaller flame naturally helps to avoid unnecessary or excessive heating. As a rule, the flame should be kept neutral. A strongly carburizing flame is undesirable as it modifies the corrosion resistance of the metal, increases the hardness and brittleness, and does not give the same color to the polished weld-metal. If the operator experiences difficulty in maintaining this neutral flame, he should favor an adjustment with a very slight excess of acetylene, rather than an excess of oxygen. The neutral flame protects the molten weld-metal from reactive welding gases and from the atmosphere.

For these reasons proper adjustment of the flame is important in maintaining the chromium content in the uncombined form (which is necessary for the best corrosion resistance) and in promoting sound welds free from oxides or detrimental carbides.



Hundreds of Thousands of Automobile Windshield Wings Have Frames Fabricated of Stainless Steel by Oxyacetylene Welding

Refrigerator Units with Oxyacetylene Welded Stainless Steel Linings Were Built to Order for Use in a School. Columbium-Bearing Rod and 20-Gage Stainless Steel Were Used



WELDING RODS

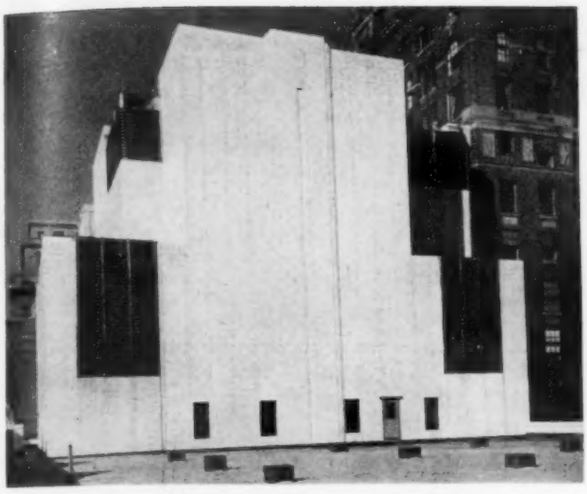
To maintain full corrosion resistance of the welds, it is desirable to use welding rods containing 1 or $1^1/_2$ per cent more chromium than the base metal. This allows for the slight oxidation losses that are apt to occur even when the metal is protected by the neutral flame. For the same reason, the welding rod can be slightly higher in manganese and silicon than the base metal, so that these necessary elements will not be excessively reduced by oxidation. Stainless steel flux will satisfactorily remove the small amounts of chromium oxide that are formed. This flux is always used, with occasional exceptions in the case of flanged joints.

It is better to use wire of the correct composition than strips cut from the metal being welded, because strip is more readily oxidizable, and it tends to assume a spiral form which precludes a uniform feed. When the composition of the base metal is not known, strip cut from the sheet to be welded does have the advantage that its composition will be approximately correct.

POSITION OF BLOWPIPE

The tip of the inner cone of the flame should be kept in contact with the pool of molten metal. This prevents oxidation. The angle at which the flame is held should be steeper than in the case of carbon steel. About 80° with the horizontal is recommended.

The welding rod, when not being fed in, should not be held in contact with the outer flame, but should be withdrawn completely. When it is held in the flame it should be held in or near the inner cone.



Over 8000 Lb. of Stainless Steel in This Building Were Fabricated and Installed by Oxyacetylene Welding. This View Shows the Intake Louvers

JOINT DESIGN

Thin metal that can be easily flanged is suitable for flange welding. Metal up to $^1/_{16}$ inch (16 gage) can be satisfactorily welded with a butt-type joint, while a veed joint is preferred for metal between $^1/_{16}$ inch and $^1/_4$ inch. Above $^1/_4$ inch in thickness, corrosion-resistant steels are preferably joined with double vees. Forehand welding is preferable for thin sheet, especially that under 0.04 inch (20 gage). Thick sheet can be welded by the backhand method.

FINISHING TREATMENT

Corrosion-resistant chromium steels owe much of their corrosion resistance to the building up of an adherent invisible oxide film on the surface. After welding, this building up of the surface oxide film can be hastened by an immersion in cold 20 per cent nitric acid (specific gravity 1.12) for 30 min., or at 150° F. for 15 min. This is called passivating. To prevent discoloration it should be followed immediately by a thorough washing in cold water. Passivating does not affect the appearance of the finished surface and should follow, wherever possible, the grinding and polishing of a weld. This treatment also removes any embedded foreign particles that might otherwise cause discoloration.

GROUP I—WELDING STRAIGHT CHROMIUM STEELS (AIR-HARDENING)

The straight chromium steels of the air-hardening type are welded in essentially the same way regardless of whether they belong to the stainless variety, or the kind that, though not stainless, has improved corrosion resistance. Steels containing less than approximately 14 per cent chromium are generally air-hardening. Those containing more than 14 per cent chromium can be considered as air-hardening if the percentage of chromium minus 17 times the percentage of carbon is less than 12.5. Actually, this is only a rough guide as there is no very sharp boundary between non-air-hardening and air-hardening high-chromium steels. Steels that otherwise might be considered as air-hardening should be classed as non-air-hardening if they are made non-air-hardening by the addition of suitable amounts of columbium. As a general rule, air-hardening chromium steels for welding should contain less than 0.12 per cent carbon.

SELECTION OF RODS

Unless color matching or special service requirements call for rod of a composition similar to that of the base metal, an austentic chromium-nickel steel welding rod* is recommended for general use with these chromium steels. If the welded part is to be used for service up to 700° F. (375° C.) an 18–8 chromium-nickel stainless steel rod* can be used; for higher temperatures, it is desirable to use a 25–12 chromium-nickel columbium-bearing steel rod.

PREHEATING AND ANNEALING ARE IMPORTANT

Preheating locally or entirely to 300 to 400° F. (150 to 200° C.) is desirable to prevent these steels from air-hardening excessively and possibly cracking during welding. Immediately after welding and without allowing the part to cool below preheating temperature, the welded part should be annealed. Tables 1 and 2 show the effect of annealing on the 4 to 6 per cent chromium steels.

The tendency to brittleness in the weld-metal of the air-hardening chromium steels can be alleviated by local blowpipe annealing for 3 min. at 1200 to 1400° F. (650 to 760° C.). This is illustrated in Table 3. The possibility of more accurate temperature control makes furnace annealing preferable whenever it is convenient.

Although annealing will improve both the ductility and the corrosion resistance of the air-hardening steels, there are many applications in which the welds are sufficiently resistant to corrosion even without annealing. Equipment used for service under mildly corrosive conditions or at elevated temperatures seldom needs to be annealed. Table 4 lists the physical properties for both blowpipe-annealed and furnace-annealed welds in 18-gage air-hardening stainless steels.

GROUP II—NON-AIR-HARDENING HIGH-CHROMIUM STEELS

Corrosion-resistant steels that are not air-hardening or that are slightly air-hardening either contain so much

Effect of Columbium in Reducing Air-Hardening Tendencies Is Shown in Tensile-Test Specimens of 6 Per Cent Chromium Steel. (Left) With 0.70 Per Cent Columbium—Shows Elongation Before Rupture. (Right) No Columbium—Brittle Fracture

* Columbium Bearing 18–8 Stainless Steel Welding Rod.

chromium that they remain ferritic despite their carbon content, or else they are made non-air-hardening by the addition of such elements as columbium or titanium. To offset the hardening influence of the carbon the percentage of chromium minus 17 times the carbon content should be more than 12.5. In any case, the chromium content should be more than 14 per cent.

The varieties which are made non-air-hardening by the addition of columbium can contain any amount of chromium, and their columbium content should be at least six times the carbon content. If it is not, the steel will not be entirely non-air-hardening. These steels require no heat treatment if welded with a 24 or 25 per cent chromium, 12 per cent nickel rod.

Table 1—Effect of Maximum Heating Temperature on the Brinell Hardness of Air-Cooled 4 to 6 Per Cent Chromium, 0.5 Per Cent Molybdenum Steel

Carbon, Per Cent	1499° F. (815° C.)	1598° F. (870° C.)	1697° F. (925° C.)	Welding Temp.
	Brinell Hardness when Air-Cooled from			
0.23	159	393	444	450 (approx.)
0.12	143	350	363	450 (approx.)
0.05	131	253	302	450 (approx.)

Table 2—The Effect of Annealing on the Hardness of Welds in 4 to 6 Per Cent Chromium Steels

Annealing Temperature	Time	Brinell Hardness
. . . (air-cooled)	. . .	340 to 430
1300° F. (730° C.)	4 hr.	200
1600° F. (870° C.)	Cooled 50° F. (28° C.) per hr. down to 1292° F. (650 to 700° C.); air-cooled	140

Table 3—Tensile and Bend Tests on Blowpipe-Annealed Oxyacetylene Welds in 4 to 6 Per Cent Chromium Steel Plate*

Condition of Sample	Yield Strength, Lb. per Sq. In.	Max. Strength, Lb. per Sq. In.	Elongation in 2 In., Per Cent	Free Bend Angle, Deg.
As-Welded	30,000–40,000	55,000–65,000	4 to 8	45 to 90
Blowpipe-Annealed	30,000–40,000	55,000–65,000	8 to 12	120 to 180

* Composition of plate; 5.51 per cent chromium; 0.07 per cent carbon; 0.56 per cent molybdenum; 0.37 per cent titanium. Welds were made in 1/4-inch annealed plate. Composition of welding rod was 6 to 8 per cent chromium with columbium.

PREHEATING

Preheating to 200 to 400° F. (90 to 200° C.) is always recommended for these steels unless the sheets are thin. This is especially important in cold weather. When mechanical treatment is impracticable, the rather low ductility of these steels in the as-welded condition makes it desirable to locate the joints where they will be subjected to the least stress.

WELDING TECHNIQUE

If tack-welds are used to hold the pieces to be joined and control their expansion and contraction, tack-welding should be done with austenitic chromium-nickel rod even when a rod similar in composition to the base metal is used for the finished weld. The number of tack-welds depends on the length and type of joint to be made, varying from one every inch to a much greater separation. After tack-welding, stainless steel flux should be applied, and the weld finished with a rod that is slightly higher in chromium content than the base metal. During the welding operation, the tack-welds should be chipped out.

The ductility of welds can be improved by peening the joint while still hot. Using as small a flame as possible and welding as quickly as possible will also help keep the grain growth to a minimum. In addition, the tendency of these steels toward brittleness can be eliminated by the use of nitrogen-bearing chromium steel.

Table 4—Physical Properties of Welds in 18-Gage Air-Hardening Stainless Steels*

	Unwelded	As-Welded	Blowpipe-Annealed**	Furnace Annealed**
Yield Point, Lb. per Sq. In.	53,000	32,400	55,350	45,400
Ultimate Strength, Lb. per Sq. In.	78,200	43,700	76,400	72,900
Elongation, Per Cent	36.0	4.0	6.5	7.6

* Typical Analysis: chromium 12.30 per cent, carbon 0.11 per cent, manganese 0.33 per cent, silicon 0.39 per cent.

** Heat treat at 1292° F. (700° C.) for 3 min. with blowpipe if annealing furnace is not available.

Table 5—Physical Properties of Welds in 18-Gage Non-Air-Hardening Chromium Steels*

	Unwelded	As-Welded	Blowpipe-Annealed†	Furnace-Annealed†
Yield Point, Lb. per Sq. In.	64,700	41,500	45,100	64,200
Ultimate Strength, Lb. per Sq. In.	77,800	53,400	65,300	73,250
Elongation, Per Cent	31.0	1.0	4.0	10.5

* Typical analysis: chromium 17.21, carbon 0.08 per cent, manganese 0.29 per cent, silicon 1.05 per cent.

† Anneal at 1202 to 1382° F. (650 to 750° C.) for 3 min. followed by air-cooling.

ANNEALING

While all of the steels in the group are subject to grain growth in the welds, only those with less than about 24 per cent chromium can be improved in ductility by annealing. The annealing should be done at 1200 to 1350° F. (650 to 750° C.). Table 5 lists the properties of welds in 18-gage non-air-hardening chromium steels which have been both blowpipe-annealed and furnace-annealed.

GROUP III—AUSTENITIC CHROMIUM-NICKEL STEELS

The austenitic chromium-nickel steels are perhaps the most popular of all the corrosion-resistant steels. Typical of this group are the 18–8 stainless steels which contain approximately 18 per cent chromium and 8 per cent nickel.

An austenitic structure in ordinary steels exists only at elevated temperatures. However, this structure exists at room temperature in the high-chromium-nickel steels when the steel is cooled rapidly from a high temperature—ranging from 2000 to 2100° F. (1100 to 1150° C.)—preferably by quenching in water. If the quenched steel intentionally or by chance undergoes subsequent

heating to a temperature ranging from 800 to 1600° F. (430 to 870° C.), the uniformity of its structure will eventually be destroyed. The welding technique and subsequent heat-treatment of these steels are based largely on this fact.

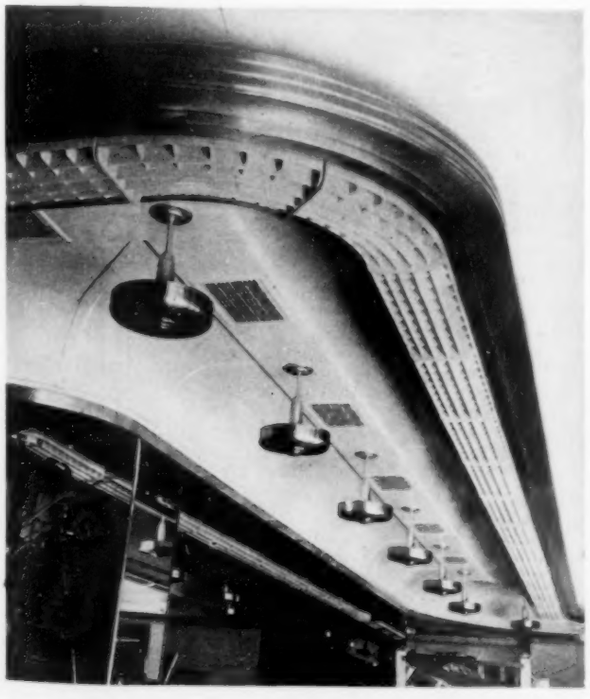
WELDING TECHNIQUE

Columbium-bearing 18–8 stainless steel welding rod is suitable for all austenitic chromium-nickel stainless steels whose alloy contents are not higher than those of the rod. Steels with higher alloy contents should be welded with rod similar in composition. With these steels particular care must be employed to be sure that the edges to be welded are well covered with stainless steel flux.

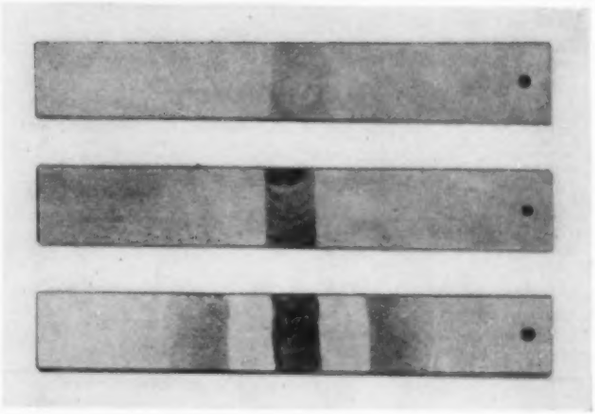
Since the coefficient of expansion of the austenitic chromium-nickel stainless steels is 1.5 to 1.8 times that of mild steel, particular care must be paid to expansion and contraction, which can be controlled by the use of jigs and fixtures, chill plates and sometimes by the careful preheating of large surfaces in special jigs. In 16-gage material and thinner, this is not a serious problem and the economics of metal deposition are particularly favorable to oxyacetylene welding.

In welding the heavier gages, good results have been obtained by tack-welding the ends, only, and using separators (wedges) about 8 inches ahead of the blowpipe. When within 2 inches of the tack-weld, the seam weld is allowed to cool and the tack-weld cut out before finishing the weld.

For short seams, it is well to tack-weld one end and start welding at the other end. Longer seams should have more tack-welds. Uniformity in speed is necessary—welding at too fast a rate tends to close the gap; too slow, to open it. Wherever possible, the weld should progress toward an edge instead of away from it.



Shop Fabricated by Oxyacetylene Welding, Ventilating Hoods and Ducts Were Erected Over the Equipment in a Cafeteria and Soda Fountain



Chromium-Nickel 18–8 Stainless Steel Weld Specimens After Exposure to a Hot Nitric-Hydrofluoric Acid Solution. (Top) Plain 18–8, as-welded. (Middle) Plain 18–8, Full Annealed. (Bottom) Columbium-Bearing Weld-Metal and Plate, As Welded

HEAT TREATMENT

For many uses, particularly with the low-carbon varieties of the austenitic stainless steels, subsequent heat treatment is not necessary. Welded stainless steel is satisfactory in countless household, hotel and laundry applications, and many others in which resistance to atmospheric oxidation is the prime requisite.

It is well to remember, however, that welding naturally produces a temperature gradient in the article being welded ranging from the melting temperature at a weld to room temperature at some distance removed from the weld. Some portion between these two extremes will have been subjected to the critical temperature range mentioned previously (800° to 1500° F.). Unless countermeasures are taken, this zone will have reduced corrosion resistance. Therefore, in the case of more severe chemical service, a heat treatment will be necessary with ordinary 18–8 stainless steel. Full resistance of the weld zone may be restored by heating the entire welded piece to about 2000° F. (1100° C.) and quenching in water. Very thin pieces can be quenched rapidly in air.

For parts that cannot be easily moved because of their size or that cannot be removed from an installation, so that it is impossible to heat treat them after welding, use must be made of columbium steels which do not become susceptible to these changes at any temperature range.

COLUMBIUM STEELS NEED NO HEAT TREATMENT

The columbium stainless steels are welded in essentially the same manner as the plain stainless steels. The principal difference is that they require no heat treatment after welding. Although the base metal can be stabilized with either columbium or titanium, the welding rod should always be columbium-bearing as the loss through oxidation is less with columbium than with titanium.

FINISHING WELDS DEPENDS ON USE

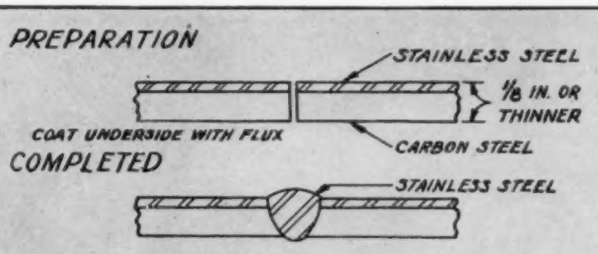
There are three common types of finishing treatment. In the case of polished stainless steel it is necessary to grind and polish the weld. When no polish is required, it may still be necessary to remove scale by sandblasting or treating with acid (pickling). Welded assemblies for

severe corrosive service should be annealed and pickled.

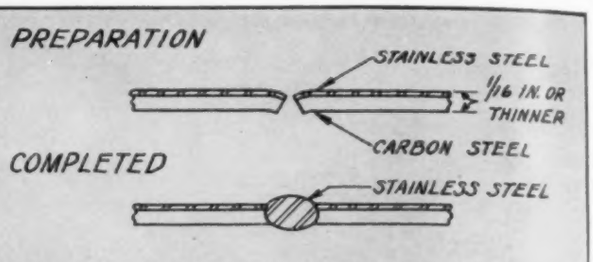
Table 6 lists figures for tensile and bend tests on oxy-acetylene welds in 1/4-inch annealed plate of 18 per cent chromium, 8 per cent nickel steel.

WELDING STAINLESS-CLAD STEELS

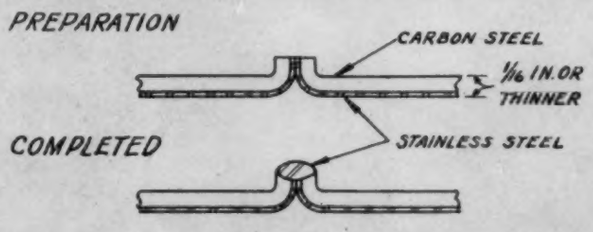
Stainless-clad steel is a two-ply material consisting of a layer of ordinary stainless steel sheet on a layer of mild



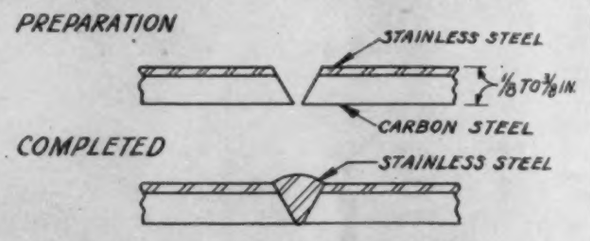
The preparation and completion of a butt-type joint in 11-gauge (1/8-in.) stainless-clad steel. The underside should be coated with flux



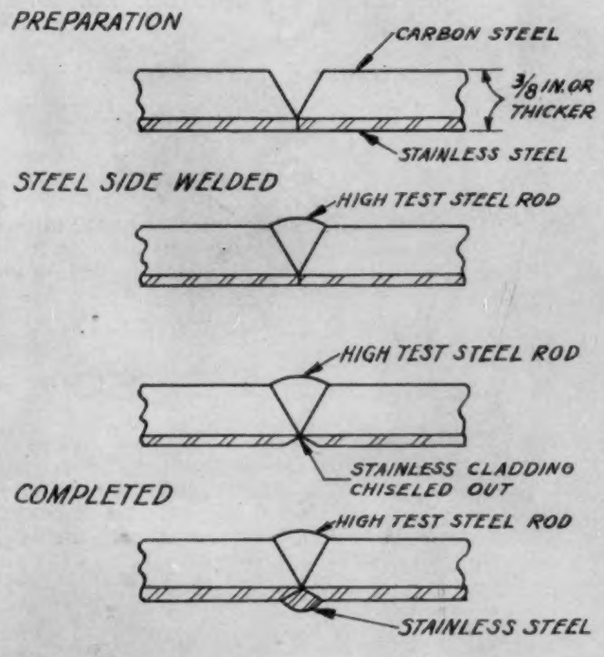
The preparation and completion of a flange butt-type weld, or "depressed edge" weld in stainless-clad steel not thicker than 16 gauge



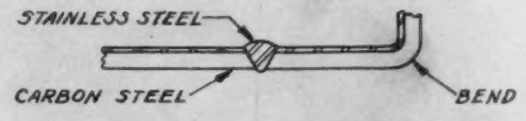
The preparation and completion of a flange joint in stainless-clad steel not thicker than 16 gauge. This is often used in fabricating piping



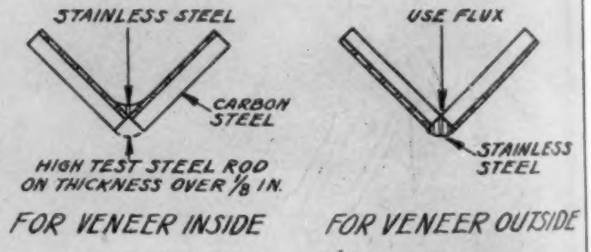
The preparation and completion of a single vee joint in stainless-clad steel which is between approximately 1/8 in. and 3/8 in. in thickness



The preparation and completion of a double vee joint in stainless-clad steel thicker than 3/8-in.



Recommended weld location for corners on stainless-clad steel thin enough to be bent



Type of corner weld for stainless-clad steel that cannot be bent. Steel welding rod is used on the outside when sheet is over 1/8-in. thick

Table 6—Tensile and Bend Tests on Oxyacetylene Welds in 1/4-Inch Annealed Plate of 18 Per Cent Chromium, 8 Per Cent Nickel Steel

Cr	Ni	C	Cb	Ti	Yield Strength, Lb. per Sq. In.	Max. Strength, Lb. per Sq. In.	Free-Bend Test, Deg.
*18.22	8.91	0.07	—	—	39,200	76,500	180 (no cracks)
†18.75	9.02	0.10	0.98	—	37,200	75,400	180 (no cracks)
†17.42	8.88	0.07	—	0.47	41,000	75,500	180 (no cracks)

* Welding rod same analysis as plate.

† Welding rod analysis—18.34 per cent chromium; 9.01 per cent nickel; 0.07 per cent carbon; 0.81 per cent columbium. All samples tested in as-welded condition.

steel at least four times as thick. Although the stainless layer should, in most cases, consist of a metal containing either columbium or titanium, the mild steel backing acts as a chill plate to conduct away excess heat from the relatively thin stainless steel layer. Intergranular corrosion will, therefore, generally not be much of a problem even if the stainless steel layer does not contain these elements. For severe service, however, columbium-bearing steel is recommended. Because of the difference in expansion rates between the two layers, the possibility of warpage should be considered. For this reason it is desirable to avoid heat treatment unless absolutely necessary.

JOINT DEPENDS ON THICKNESS

The accompanying drawings show the recommended types of joint design. It will be seen that for material thinner than 16 gage either the flange butt weld (depressed edge weld) or the ordinary butt weld can be made with stainless steel welding rod. For material up to 1/8 inch in thickness, the flange joint or the butt-type joint can be made also with stainless steel welding rod. For material 1/8 inch to 3/8 inch thickness a single-vee weld is made with stainless steel rod from the stainless-clad side. For thicker material a double vee weld is made; the vee in the mild steel is made and welded first, then the vee in the stainless cladding is chipped out and welded with stainless steel rod. With thin material it is desirable to locate the joint so as to avoid welding at the corners when the stainless cladding is inside the corner; but with thicker material such practice is permissible. The reason for this is that the flame inside the corner is apt to heat a wider area than would be necessary. On thicknesses over 1/8 inch, the corner weld with the veneer inside should be welded from the steel side first with High Test steel welding rod. Stainless steel flux should be applied to the inside. With veneer on the outside, weld with 18–8 rod.

In all cases when the welding is done first on the stainless steel side, flux should be painted on both sides.

Lap welds are to be avoided. A neutral flame and the forehand technique are used just as they are with other stainless steels. Flux is not applied to the carbon steel side or to the rod when a double vee weld is made. Procedure control is important. In designing joints, it should be remembered that flanging stiffens the sheet, reducing its tendency to warp or buckle.

CASTINGS

Corrosion-resistant steel castings should be preheated to a red heat to avoid cracking, and cooled slowly after welding. Rod similar to the base metal or 18–8 should be used. An excess acetylene flame aids welding and is permissible.

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Welding Handbook. First edition published by the AMERICAN WELDING SOCIETY, 33 West 39th St., New York. Comprehensive book of 1211 pages. Prepared by some 90 authors and reviewed by 237 reviewers. Covers every phase of welding. Price to members, extra copies $5.00. Price to non-members $6.00 in United States; $6.50 elsewhere.

Welding Society ACTIVITIES—Related Events

THIRTEENTH INTNL. ACETYLENE CONGRESS

The last International Congresses, which provided an international exchange of ideas for furthering the scientific and technical bases of the entire realm of gas welding, were held in Rome (1934) and London (1936). The thirteenth International Acetylene Congress will be held in Munich, June 26 to July 1, 1939.

The object of the Congress will be to promote exchange of techno-scientific information on gas cutting, gas welding, and hardening. Furthermore, there will be an international exposition in these fields at the Congress. Motion picture shows are also planned with the same object.

The following sections, headed by well known experts will be formed:

Section 1. Carbide Production.

Section 2. Acetylene as initial raw material for organic compounds.

3. Acetylene Lighting (Acetylene Lights, Marine Signals, etc.).

4. Gases, particularly acetylene and oxygen.

5. Apparatus and machines for welding.

6. Metallurgy of welding, cutting and hardening.

7. Technique and utilization of gas welding and cutting.

8. Technique and utilization of autogenous hardening.

9. Mechanical Testing.

9a. Non-Destructive Testing.

10. Welding and Cutting Specifications.

11. Improvements.

12. Accident prevention and protection of workmen.

13. Motion Pictures.

14. Exposition.

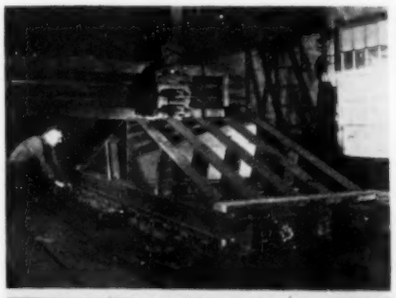
Members of the AMERICAN WELDING SOCIETY desiring to present papers before this Congress should get in touch with Dr. H. Holler, General Secretary, XIII International Acetylene Congress, Bennigsenstrasse 25, Berlin-Friedenau, Germany. Papers for the Congress must be received by Feb. 1, 1939.

WELDED FLOOR PLATES SAVE 25%

These welded floor plates recently fabricated in the East Pittsburgh shops of the Westinghouse Electric & Manufacturing Company are for use with special plant equipment.

Figure 2 shows a top and bottom section of welded steel floor plates for use in testing heavy equipment. Figure 1 shows a bridge floor plate being machined. This latter type of construction was employed because this plate spans a tunnel under the floor.

Fig. 1—Bridge Floor Being Machined



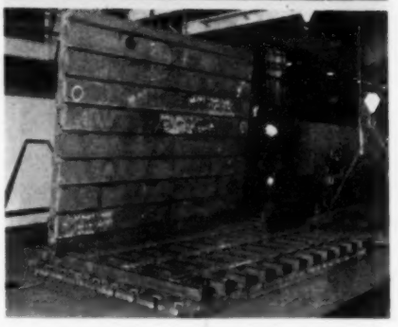


Fig. 2.—Top and Bottom Section of Steel Floor Plates

Forty-one hundred square feet of floor plates which were strain annealed and machined on the top surface were used effecting a savings of approximately 25% through the use of welded fabrication.

WELDED PIPING

Blaw-Knox Company, through its Power Piping Division, is completing ahead of schedule the general mill piping for the new Irvin Works of Carnegie-Illinois Steel Corporation. This piping installation is one of the largest ever undertaken in connection with an industrial project. It involves about 60 miles of piping, ranging in size up to 48 inches in diameter, and includes lines for high and low pressure steam; filtered, raw and city water; hydraulic systems up to 3500 pounds pressure; air, fuel and acid supply; and circulating oil and grease systems. Most of the lines were pre-fabricated to facilitate the field work, and all lines were joined by electric welding except for connections at valves or to machinery equipment.

COMMITTEE ON RADIOGRAPHIC TESTING

In recent years the use of X-rays and radium for inspection of important engineering structures has increased considerably. This is especially the case where failure would result in heavy economic losses, or where human safety is involved.

Recognizing this the A.S.T.M has recently organized Committee E-7 on Radiographic Testing. Following the usual procedure of the parent body the Committee membership includes producers of articles subject to X-ray inspection, users of products subjected to this inspection, makers of X-ray apparatus and radium capsules, and research workers.

The fundamental purpose of the Committee is to place the industrial use of radiography on a rational common sense basis.

WELDING ALUMINUM

The rapid growth in the use of aluminum and its alloys has brought about a demand for methods particularly adapted to the joining of these materials. Welding provides a means of making permanent joints and repairs on aluminum and aluminum alloy products. Specific instructions for the welding of aluminum and its alloys are given in the new pamphlet just issued by the Aluminum Company of America, Pittsburgh, Pa., with a brief discussion of the principles involved.

The needs of the practical welder have been kept in mind throughout this booklet and no extended academic discussion of the problem has been attempted.

THE WELDING ENCYCLOPEDIA—NINTH EDITION

This practical reference book dealing with all of the welding processes in commercial use has just made its ninth appearance in a completely re-edited volume prepared by Stuart Plumley. Since the publication of the eighth edition more than six years have elapsed during which time the welding industry has witnessed a number of major engineering developments. Since the publication of the first edition in January 1921, under the editorship of L. B. Mackenzie, the book has kept in step with the rapid growth of the industry.

A feature of the new edition is the arrangement of subject matter in alphabetical order so that reference data can be located as quickly as in a dictionary or telephone book without consulting an index. A large part of the Ninth Edition has been

completely rewritten, particularly the sections dealing with arc welding. As in the past editions, the new Ninth Edition includes a complete list of trade names. Company names have been inserted alphabetically, listing in each case all of the trade names used on that company's products. This feature enhances the value of the Welding Encyclopedia as the standard trade directory of the industry.

It is evident from checking through the pages of the Ninth Edition that the editor has emphasized the practical and commercial aspects of welding. The book has been designed primarily to meet the needs of the practical man who must apply the welding processes. Although the book contains considerable engineering data, less emphasis has been placed on the purely technical and research aspects of welding, with the purpose in mind of making the book of maximum value to the practical man in the shop or out on the firing line in the field. The Ninth Edition is thus presented to the welding industry as a practical book to supplement the excellent engineering text-books and engineering publications currently available.

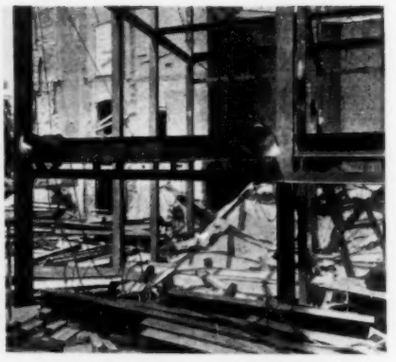
From the following list of principal subjects, the reader will gain a bird's eye picture of the contents of the Ninth Edition:

Acetylene—Agricultural Implements and Machines—Aircraft Structures—Aircraft Welders—Aluminum—Annealing—Automotive Repair—Boiler Sections—Boiler Welding—Business Methods—Calcium Carbide—Carbon—Cast-Iron—Copper and Its Alloys—Cutting Metals—Electric Arc Welding—Electric Resistance Welding—Firebox Welding—Flame Hardening—Galvanized Iron—Hard Facing—Jigs and Fixtures—lead Welding—Locomotive Cylinders and Frames—Machinery, Malleable Iron—Manganese Steel, Metallography—Metal Spraying—Monel Metal—Nickel—Nickel-Clad Steel—Nickel Alloy Steels—Oxyacetylene Welding—Pipe Welding—Plow Shares—Preheating—Rail Bonds—Rail Joints, Rules, Regulations and Codes—Sheet Metal Welding—Silver Soldering and Brazing—Soldering—Specifications for Materials—Stainless Steel—Structural Steel, Surface Hardening—Tables and Charts—Tank Welding—Testing and Inspection of Welds, Thermit Welding—Tools, Welding of—Track Welding—Training Operators—Welding Rods and Electrodes—Welding, Symbols—White Metal—Supplement—Catalog Section.

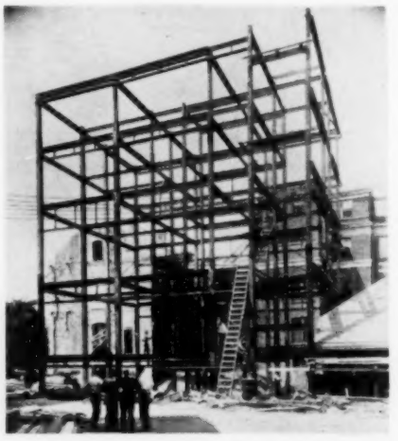
Copies of the Ninth Edition of The Welding Encyclopedia (696 pages, $5^5/_8$ in. x $8^3/_4$ in.) can be obtained by ordering from THE AMERICAN WELDING SOCIETY. The book will be sent anywhere in the United States, Canada or foreign countries for $5.00 postpaid. As a service to members through quantity purchase the society has made arrangements to supply the Encyclopedia to its own members at $4.00.

STEEL WORK RISES FOR SCHENECTADY'S SEVENTH WELDED BUILDING

Erection crews of General Erectors, Inc. have the steel framework for a four-story addition to Ellis Hospital, Schenectady, N. Y., partly in place. Following behind the steel workers, operators from Scott Welding Service, Long Island City, N. Y., are permanently joining the various members by welding. Preliminary work on the flooring is expected to begin within the very near future. The construction of the new building is under the direction of James E. Lowe and Sons, Inc., Schenectady contractors.



Operators Welding Steel Members



Side View of Addition to Ellis Hospital Showing Section of Steel Framework in Place

This hospital addition is the seventh building with an all-welded steel frame to be erected in the city. Previous welded buildings include the local Y.M.C.A. and Y.W.C.A., the city hall, a large printing establishment, the new state armory, and the new building for WGY, the General Electric broadcasting station.

WELDING PAPERS

The Welding Division of the German Society of Engineers (VDI) held a general meeting in Stuttgart in May at which the following papers were delivered:

Professor O. Graf—The Effect of the Shape of the Welded Joint in Steel Construction

Dr. E. H. Schulz—The Sensitivity of Low-Alloy Steels to Welding Cracks

Dr. M. Ulrich—Boiler and Tank Construction

E. Stursberg—Pipe Welding

E. Berndt—Welded Machine Tools

K. H. Seegers—Welded Office Building and Bridges

LINCOLN PRIZE PAPERS

The papers submitted in the $200,000 Award Program of The James F. Lincoln Arc Welding Foundation are being judged at Cornell University, Ithaca, N. Y., by the Foundation Jury of Award which convened July 2.

Thirty jurors from engineering departments of various colleges and universities will judge the entries under the direction of Dr. E. E. Dreese, of Ohio State University, Chairman of the Jury of Award.

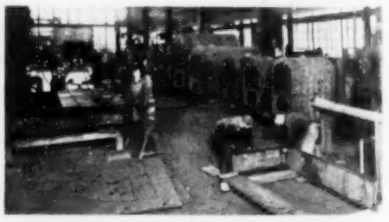
To assist in properly appraising the merits of any paper, the Jury of Award will consult experts or outstanding authorities in the various classifications of the Award Program.

The Jury of Award, will remain at Ithaca until it completes the selection of the winning papers. It is expected that the announcement of award winners will be made about the middle of September.

SPEED AND QUALITY OF BOILER FABRICATION IMPROVED BY A-C WELDERS

Speed of operation has gone up while maintenance and power costs have gone down in the Geneva, N. Y. division of the Burnham Boiler Corporation, manufacturer of low-pressure heating boilers. These results were brought about by two major programs: first, the substitution of equipment that was best suited for the work; second, improved production control and similar foresighted changes. The benefits obtained paid for the new equipment in eight months.

Boiler Being Repaired by Alternating Current Arc Welding



Interior View of Shop (Photo Courtesy General Electric Co.)

About three years ago, A.C., transformer-type welders, including two 500-ampere units, were substituted for the D.C. units previously used. Records kept since the

changeover show a 75 per cent reduction in cost per foot of weld. Factors in this reduction are a 60 per cent decrease in welding time per boiler, 35 per cent out in the amount of electrode used and a 70 per cent lowering of power and maintenance costs. Welding operators express satisfaction due to the increased speed and ease of work.

With the elimination of "magnetic blow" by the use of alternating current, operators are able to work in boiler corners much more accurately and easily.

SCOW BUILT IN FOUR SECTIONS ON WEST COAST

One of the first instances in which a scow has been constructed in watertight sections which were launched and then bolted together in the water is a vessel recently completed by the Pacific Coast Engineering Company, Oakland, California for the U. S. Engineers.

Construction of the scow is shown in the accompanying illustrations. Figure 1 shows the four sections of the scow being completed. Figure 2 shows the hull plating being applied.

The hull framing is composed almost entirely of standard rolled shapes and plates. Some members were formed. The method of construction was to cut the steel shapes and plate to required sizes, form any members necessary, erect the framework and bolt it together until checked for position and alignment. After the frame was checked, all connections were fused together, forming one integral piece of steel.

Construction of this vessel in watertight sections would not have been economically possible without welding. With arc-welded construction, using continuous seam welds for the hull plating, the vessel automatically becomes leakproof. The work, both of construction and caulking, is, thus, done in one single operation.

Added to leakproof construction, arc welding kept deadweight considerably below conventional methods while providing greater strength and rigidity in the finished structure. The low deadweight of the vessel will be reflected in economical operation. Permanently leakproof construction will eliminate maintenance costs for seam caulking and repairing the effects of corrosion.

The sections of the scow were constructed entirely by arc welding. After the four sections were completed they were loaded on to a vessel, shipped to their destination and bolted together in the water.

Fig. 1—Four Water-Tight Sections of Scow Constructed by Arc Welding at the Pacific Coast Engineering Company, Oakland, California, for U. S. Engineers

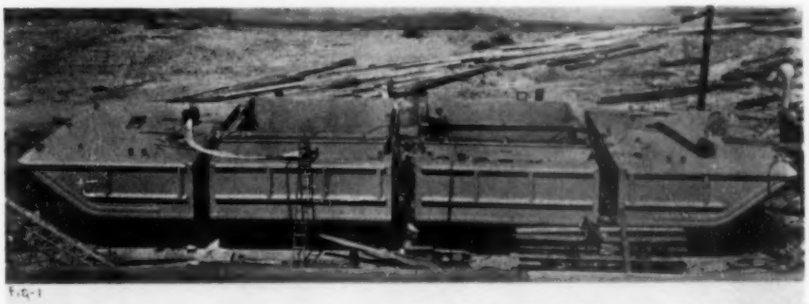
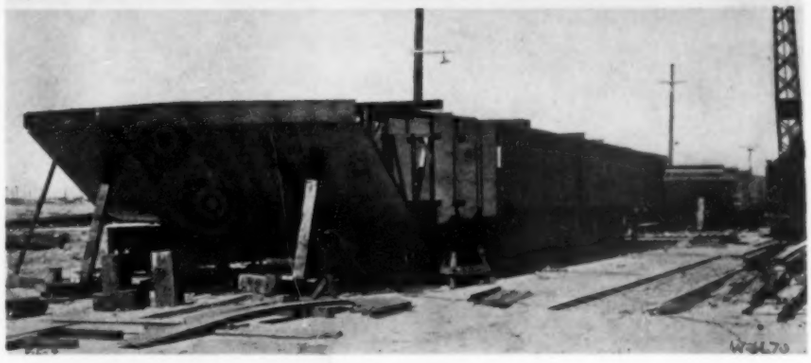


Fig. 2—Hull Plating Being Applied to the Sections of a Scow at Pacific Coast Engineering Company



EMPLOYMENT SERVICE BULLETIN

SERVICES AVAILABLE

A-282. Young Industrial Engineer with four years Machine Shop and three years Welding Design and Steel Fabrication experience, will be available September 15, 1938. Can handle Welding Design, Supervision of Welding, Instruction and Sales Engineering work.

A-283. Welder with gas and automatic welding experience. Have had 1 year's work in shipyard, all positions. Free to go anywhere, preferably in the Northeastern states. Can furnish good references.

A-284. Electric and Acetylene Welder. Six months' experience in shop, where now employed. Have had considerable practice.

SECTION ACTIVITIES

CLEVELAND

The first meeting of the season will be held on October 5th. Mr. Robert E. Kinkead will speak on "Welding in the Steel Industry." Definite place for the meeting will be announced later.

PITTSBURGH

The result of the Ballot Election held August 10th of the Pittsburgh Section is as follows:

Chairman—Leon C. Bibber, Welding Engineer, Carnegie-Illinois Steel Corp.

Vice-Chairman—George F. Wolfe, Chairman Welding Committee, Dravo Corporation Engineering Works Division.

Secretary—J. F. Minnotte, Secretary-Treasurer, Minnotte Brothers Company.

Executive Committee—One Year Term.

A. E. Marble, Jones & Laughlin Steel Corp.

W. R. Pearsons, The Lincoln Electric Company.

S. D. Edsall, Air Reduction Sales Company.

Executive Committee—Two Year Term.

R. B. Lincoln, Pittsburgh Testing Laboratory.

C. E. Loos, American Bridge Company.

Milton Male, United States Steel Corporation.

The new officers will be inducted into office at the opening meeting in October.

NORTHWEST SECTION

The Program Committee, consisting of Messrs. R. H. Newton, Chairman, The Lincoln Electric Company, T. P. Hughes, University of Minnesota and G. W. Irwin, Air Reduction Sales Company, has been exceptionally busy the past few weeks arranging the program for the coming fall and winter season of meetings. The Committee has been fortunate in having a number of nationally known authorities scheduled to come to Minneapolis to present papers and discuss problems covering the most recent developments in the art and science of welding.

The third Wednesday of each month has been fixed as the regular meeting day of the Section, and it is hoped that the Minnesota Union at the University of Minnesota will be available for the Section's meetings this year as it was last.

At the last meeting of the Executive Committee, the activities of the Section were thoroughly discussed, and every member feels that the Section will have a very successful season.

NEW PRODUCTS

The Society assumes no responsibility for the validity of claims in this Section

PORTABLE OXYACETYLENE CUTTING MACHINE

The Linde Air Products Company, Unit of Union Carbide and Carbon Corporation, announces the Oxweld (Type CM-15) Portable Shape-Cutting Machine, the latest addition to its line of portable cutting machines.

The CM-15 combines many of the desirable features of both portable and stationary cutting machines. The machine weighs less than 200 lb. complete. All controls of the machine are conveniently located adjacent to one another. The cutting area—that is, the maximum range of blowpipe movement—is 60 inches lengthwise and 18 inches laterally. Cutting speed can be varied from $3^1/_2$ to 28 inches per min., and a speedometer indicates movement directly in inches per minute. Accurate results can be obtained within the operating limits of the machine because of careful and precise construction.

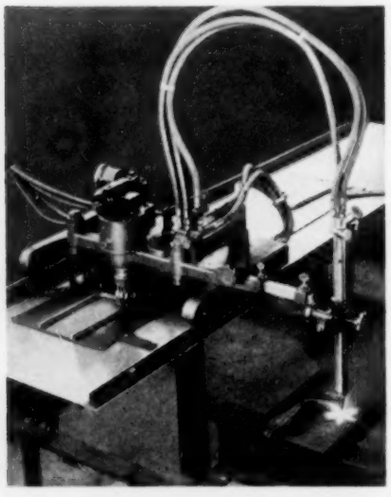
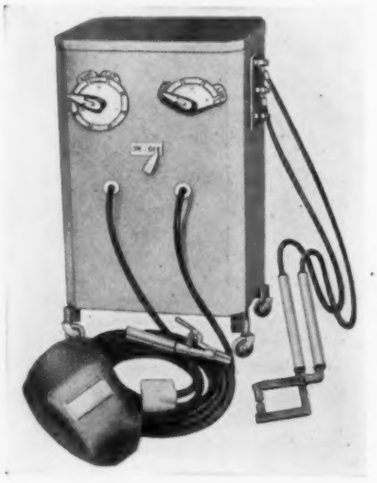


Fig. 1—Accurate Cutting of Any Number of Similar Shapes Is Possible with the Templec Tracing Attachment for the CM-15 Portable Shape-Cutting Machine

ARC AND SPOT WELDER

A combination arc and spot welder, illustrated here, is announced by Miller Electric Mfg. Co., Appleton, Wisconsin as an addition to their present line of arc welders. The spot welder is built into their regular No. 2, and No. 3 welders, with capacities of 165 amperes for the No. 2 and 220 amperes for the No. 3. These units are designed to provide arc welders of ample capacity to handle electrodes from $^1/_{16}$ inch to $^1/_4$ inch, and have 32 heat controls, enabling the operator to weld from the lightest to the heaviest metal, and with the addition of the spot welder will handle all sheet metal work. The spot welder itself has a number of heat controls for different thicknesses of metal. Special spot welding tongs, shown



with the welder, are made in different styles to handle individual applications. This new welder is patented in the United States.

DRESSING TOOL

Metallurgical Products Company, 2950 Penobscot Building, Detroit, have recently developed a very satisfactory dressing tool for spot welding tips.

The device consists of a nine-inch chrome plated ratchet handle and a double sided cutting tool with ground teeth. The construction is simple, the only other parts being a ratchet key with compression spring and a cover plate attached by two screws. Although the cutter is durable it can be replaced at reasonable cost.

This tool offers the welder speed and convenience in the dressing operation. It removes the mushroom with the greatest economy of metal and dresses the tips uniformly. The convenience of this device makes for more frequent dressing of tips. The consequent saving by the elimination of welding failures should be obvious.

A tool with finer teeth (cutting edge on one side only if desired) is available for gun welders. The operation is identical.

RONAY ARCRONOGRAPH

The Rubicon Company, electrical instrument makers, of 29 North Sixth Street, Philadelphia, Pa., has just issued an attractive booklet on the Arcronograph. This is an advanced graphic instrument used in Arc Welding for recording uniformity of arc stream, skill of welding operators and quality of weld.

LOW COST ELECTRONIC WELDING CONTACTOR

For welding mild steel products and other readily-welded metals, a new "weld-o-trol" electronic power switch for controlling the primary of welding transformers and suitable for use with existing timing devices, is announced by Westinghouse Electric & Manufacturing Company, East Pittsburgh, Pennsylvania. With either sealed-off or continuously pumped ignitron tubes, the Weld-o-trol offers an instantaneous power switch for producing uniform welds, and is free of moving parts, arcing contacts and noise. Available ratings are roughly equivalent to 300 and 600 ampere conventional welding contactors.

NEW FOUR-FLAME AND SIX-FLAME TIPS

The Linde Air Products Company, Unit of Union Carbide and Carbon Corporation announces two new multi-flame welding tips for pipe line construction by the Lindeweld process. The introduction of these two new multi-flame welding tips has resulted in increased speeds of 25 per cent or more and a corresponding reduction in gas consumption.

Fig. 1—The 4-Flame Tip for Position Welds

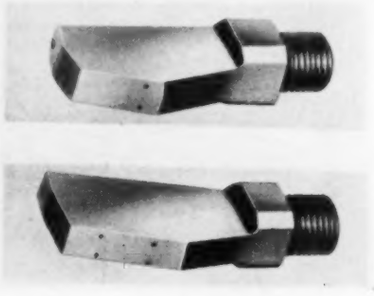


Fig. 2—The 6-Flame Tip for Rolling Welds

The four-flame tip has been designed for position welds while the six-flame tip is intended primarily for rolling welds.

The four-flame tip provides, in addition to the main welding flame, two flames for preheating the vee and a fourth flame for preheating the welding rod. The six-flame tip is similar to the four-flame tip in design with the exception that it has two additional vee preheat flames, so that four flames preheat the walls of the pipe a fifth flame preheats the welding rod, and a sixth flame does the actual welding.

NEW WILSON 100 AMPERE MOTOR GENERATOR ARC WELDER

The Wilson Welder & Metals Co., Inc., 60 East 42nd Street, New York, has announced the newest addition to their comprehensive line of machines, the Model SC, Single Operator 100 Ampere Motor Generator Arc Welder.

The machine is extremely compact and is designed primarily for light gage work, operating perfectly on the thinnest material that can be arc welded.

List of New Members

July 1 to July 31, 1938

BOSTON

Fennell, Le Barron J. (C), New England Welding Labs., Inc., 88 St. Stephen St., Boston, Mass.
Kyle, Peter E. (C), Mass. Inst. of Tech., Cambridge, Mass.
Singleton, G. R. (B), New England Welding Labs., Inc., 88 St. Stephen St., Boston, Mass.

CANADA

Cousineau, L. P. (C), P. O. Box 353, Sorel P. O., Canada.

CINCINNATI

Bergmann, Julius R. (C), P. O. Box 44, Cleves, Ohio.
Raftery, T. E. (C), Linde Air Products Co., 709 Melish Ave., Cincinnati, Ohio.

CLEVELAND

Arnold, A. C. (C), National Cylinder Gas Co., 3965 Jennings Road, Cleveland, Ohio.
Sheppard, G. W. (B), Ocean Accident & Guarantee Corp., 1431 Union Commerce Bldg., Cleveland, Ohio.

COLORADO

Clark, E. E. (D), Denver Tramway Co., 65 S. Lincoln, Denver, Colo.
Craig, R. L. (F), 3402 W. Center St., Denver, Colo.
Roberts, C. R. (B), Silver Roberts Mfg. Co., 33rd & Blake Sts., Denver, Colo.
Troutman, L. L. (F), 3038 W. Gill Place, Denver, Colo.
Waller, H. E. (B), The R. Hardesty Mfg. Co., 3063 Blake St., Denver, Colo.

DETROIT

Bieszk, Anthony (B), Bieszk Brothers Co., 37705 Plymouth Road, Plymouth, Mich.

HAWAII

Elton, M. G. (D), Beach Walk Inn, Honolulu, T. H.
Erickson, C. J. (B), 2662 Hillside Ave., Honolulu, T. H.

KANSAS CITY

Reals, Chas. L. (C), 717 Freemont, Kansas City, Mo.
Streeter, J. Reel (B), 4510 Lloyd Ave., Kansas City, Kans.

LOS ANGELES

Black, William A. (D), 6026 Falcon Ave., Long Beach, Calif.
Karge, Fritz (C), Union Oil Co. of Calif., Union Oil Bldg., Los Angeles, Calif.
Murcell, U. C. (B), U. C. Murcell, Inc., 5701 E. Leneve St., Los Angeles, Calif.
Scherrer, Robert (D), 3334 Lowell Ave., Los Angeles, Calif.

MEMPHIS

Bolin, Nick (C), Lewis Supply Co., 477 S. Main St., Memphis, Tenn.

MILWAUKEE

Binder, Henry J. (D), 2111 Slauson Ave., Racine, Wis.

NEW YORK

Becker, E. C. (B), Phoenix Engrg. Corp., 2 Rector St., New York, N. Y.
Boland, F. J. (D), 12 Clark Place, W. New Brighton, Staten Island, N. Y.
Cooke, A. E.(B), Dutchess Co., Hwy. Dept., P. O. Box 116, Poughkeepsie, N. Y.
Howe, G. E. (C), American Bridge Co., 71 Broadway, New York, N. Y.
Mott, Clement (D), 107-31 127th St., Richmond Hill, N. Y.
Petrusky, Jos. W. (D), 110 W. 11th St., Linden, N. J.
Shushansky, Hyman (D), U. S. Army Air Corps., Mitchell Field, 2nd Air Base Squad, Long Island City, N. Y.
Thomsen, S. Henry (C), 601 40th St., Brooklyn, N. Y.
Wagner, Carl A. (C), 8514 114th St., Richmond Hill, N. Y.

NORTHERN NEW YORK

Clark, Lewis S. (C), Int. General Elect. Co., Schenectady, N. Y.

WESTERN NEW YORK

Bard, J. K. (B), Bethlehem Steel Co., Buffalo, N. Y.

OMAHA

Young, E. D. (D), 4216 Lafayette St., Omaha, Nebr.

PHILADELPHIA

Huber, G. L. (B), 2940 Ellsworth St., Philadelphia, Pa.
Strayer, R. K. (B), 236 E. King St., Lancaster, Pa.
Veihl, E. E., Jr. (C), General Steel Castings Corp., Eddystone, Pa.

PITTSBURGH

McCorkle, I. B. (A), 4910 Forbes St., Pittsburgh, Pa.
Minehart, Glenn K. (C), Jourden Diesel Schools, Inc., Pittsburgh, Pa.

SAN FRANCISCO

Schroeder, Otto (C), Soule Steel Co., 1750 Army St., San Francisco, Calif.

SAN JOAQUIN

Smith, Floyd B. (C), 722 L. Street, Bakersfield, Calif.
Stutzman, N. D. (C), Kern County Union H. S., Bakersfield, Calif.

TULSA

Neet, J. G. (C), Linde Air Products Co., National Bank of Tulsa Bldg., Tulsa, Okla.

WICHITA

Plattner, Emil L. (C), Plattner Welding Supply Co., 210 E. 10th St., Coffeyville, Kans.
Sheets, C. L. (D), 1139 S. Water St., Wichita, Kans.

NOT IN SECTIONS

Akey, R. P. (B), Hobbs Mfg. Co., P. O. Box 1568, Fort Worth, Texas.
Blanchard, B. E. (B), 947 N. Oakdale, De Pere, Wisc.
Fergusson, H. B. (B), G. A. Harvey & Co., Ltd., Woolurch Road, London, S. E. 7, England.
Helper, H. H. (B), Linde Air Products Co., 828-32 Howard Ave., New Orleans, La.
McCarthy, Jim (C), 214 Wilhelmina, Kalamazoo, Mich.
Soles, Harold (F), Buchtel, Ohio.
Van Kirk, R. W. (C), General Elec. Co., 1801 N. Lamar St., Dallas, Texas.

Sustaining Companies

National Tube Company, Frick Building, Pittsburgh, Pa. Seamless and Welded Steel Tubular Products; Plain Carbon and Alloy Steels for Pipe Pressure Tubes. In a wide range of diameters, wall thicknesses and lengths for Water, Steam, Gas, Oil, Air, Sprinkler, Refrigeration, high temperature and high pressure service and other industrial uses. Quality suitable for bending, coiling, flanging and special fabrication. Ends prepared for welded, threaded or special types of connection. Made to Standard Specifications such as A. S. T. M., A. S. M. E., A. P. I. and U. S. Government.

Shawinigan Products Corporation, New York City. Pure materials and expert craftsmanship developed during a period of over forty years, assures the satisfactory performance of Shawinigan Carbide. This exact performance of the product has resulted in the growth of three huge plants and an efficient organization to deliver Shawinigan Carbide when, where and how you want it.

BEHIND YOUR GOOD TELEPHONE SERVICE IS THE

Constant Courtesy OF

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THE MAN ON THE JOB

THE MEN AND WOMEN IN THE TELEPHONE OFFICE

This country is entitled, in good times and bad, to the best possible telephone service at the lowest possible price.

A great factor in accomplishing this is the real spirit of service that has become a tradition among telephone men and women. Courtesy and efficiency are important words in the Bell System.

BELL TELEPHONE SYSTEM

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, September 1938

WELDING SILICON STEELS

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

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Summary

PHYSICAL PROPERTIES

Arc welds in silicon steel (0.12 C, 1.00 Si; or 0.20 C, 0.80 Si) have been made with the same strength as base metal (77,000 lb./in.^{2}). The bend angle was 32.5 to 84°; unwelded had nearly 180°. Weld deposits having excellent tensile strength (76,000 lb./in.^{2}) fair ductility (15.5% in 2 inches) and rotating bend fatigue limit of 28,800 lb./in.^{2} have been made with the atomic hydrogen process using a silicon steel rod (1.98 Si, 0.47 C). Fatigue tests have been made with disappointing results on rolled I beams with arc-welded reinforcing plates on the flanges. Both beams and plates were made of "silicon" steel containing 0.27–0.29 C, 1.0 Mn, 0.35 Si.

RECOVERY AND PICK UP

The recovery of silicon in oxyacetylene welding varied from 45% with a low-silicon rod (0.40% Si) to 90% in a high-silicon rod (4.5% Si). In bare wire arc welds recovery varied from 9 to 75% in a 0.40 Si rod depending on whether the carbon content was 0.34 or 0.88%. If Si was increased to 1.20% the recovery was 60% even with a low-carbon rod and nearly 90% with a 4.0% Si rod. Manganese had no effect on recovery of silicon. In atomic hydrogen welding the recovery of silicon from rods containing 2 to 4% Si is 90 to 100%. The diffusion of silicon from base metal (4% Si) to arc weld-metal is relatively slow.

FUSION WELDING PROPERTIES

Of the two welding difficulties: cracks and poor fusion, the latter is the main one in welding silicon steel. The silicon content of aircraft steels has no effect on their sensitivity to cracking during welding. The difficulty with silicon steels (1.5 Si or more) is that the silicon oxidizes in the arc and makes the slag viscous. For welded pressure vessels the silicon content is usually limited to 0.25% max. in this country and Germany, and to a trace in France.

The use of silicon in electrodes depends on adjustment of other alloys. It is often advantageously incorporated in coated electrodes. In oxyacetylene welding a ratio of 3 parts Mn to 2 parts Si in the rod (0.80 Mn, 0.55 Si, 0.20 C), produces a slag of the correct viscosity and deoxidizing power. Silicon may be detrimental to the quality of oxyacetylene welding rods unless other elements in the rod are correctly proportioned.

Filler rods containing rather high silicon have been used successfully for atomic hydrogen welding.

"Some of the apparent inconsistencies in test results on welding silicon steels will vanish when it is recalled that the term "silicon steel" refers to no less than three types of steels, as pointed out by F. T. Llewellyn (private communication, June 1938):

1. The steel containing not over 0.45 Si and 1.00 Mn, mistakenly called silicon steel by some structural engineers.
2. The steel containing over 1.00 Si, rightly called silicon steel.
3. Electrical silicon steel containing 1 to 5 Si (see section on Silicon in Steel)."

RESISTANCE WELDING

Satisfactory flash welds having good tensile properties and elongation have been produced in medium carbon steel (0.18 C) with 0.35% Si.

FORGE WELDING

Silicon steels containing 0.30 to 1.35 Si are not suited for forge welding. Successful welds have been produced in steels containing 0.1 C, 0.30 Si and 3.93 to 4.65 Si.

FLAME CUTTING

Oxyacetylene flame cutting of transformer iron with 4% Si and low carbon has been found to be possible, but the operation was slower than with mild steel. If, however, 0.2 C was present with 4 Si, the steel could not be flame cut. High-silicon iron (15 Si, 0.5 C) and silicon itself could not be flame cut with or without preheat.

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Welding Silicon Steels

INTRODUCTION

(Contributed by Walter Crafts)

SILICON is present in many industrial steels in amounts ranging from 0.03 to about 5% depending on the purpose of the addition and the field of application of the steel. A detailed statement of the effects of silicon in steel has been given by Greiner, Marsh and Stoughton (Alloys of Iron and Silicon, McGraw-Hill, 1933). Silicon is introduced in the manufacture of pig iron for its deoxidizing and desulphurizing properties and to facilitate subsequent steel making operations. The heat of oxidation of silicon to SiO_2 is utilized in both Bessemer and open-hearth processes in order to obtain suitable temperatures and the resulting SiO_2 is necessary to flux the highly basic oxide slags that are required to lower the carbon content of the charge.

Silica exists in several amorphous and crystalline forms with a melting temperature of about 1700° C., but relatively pure silica is very seldom encountered in steel. With FeO, SiO_2 forms one compound (fayalite, $2FeO \cdot SiO_2$ at 29·45% SiO_2) which with the associated eutectics gives fluid compositions at steel melting temperatures up to about 50% SiO_2. The $MnO \cdot SiO_2$ system is of a similar type and the $FeO \cdot MnO \cdot SiO_2$ slags have a wide fluid range. Acid steel is made in a silica sand lined furnace and the slag is usually manipulated to reduce silicon into the steel to effect deoxidation. In the basic process the FeO content of the metal may be reduced by initial deoxidation in the furnace with silicon or silicomanganese alloys containing 10 to 25% silicon. Final deoxidation is usually carried out in the ladle with 50% silicon ferrosilicon for additions of up to about 0.50% silicon and with 75 or 90% silicon ferrosilicon for larger additions. The heat of solution of silicon in iron is high enough so that very large (5 to 10%) additions of silicon can be added without chilling the metal. Silicon reduces the FeO content below the amount required for FeO to react with carbon and thereby prevents the formation of CO and quiets or kills the steel. With small silicon additions (0.10%) this prevents effervescence in the mold and results in a semi-killed ingot. Higher amounts completely prevent the reaction and result in large shrinkage or pipe cavities that may be confined to the top of the ingot by a refractory shrink head or hot top.

Steels that are commonly designated as silicon-treated usually contain less than 0.10% silicon. This amount of silicon effects only a partial reduction of FeO but has a stabilizing effect that minimizes the tendency toward oxide segregations in rimmed and semi-killed structural steels by supplying an acid flux for the basic iron and manganese oxides. Such small amounts of silicon have no appreciable effect on tensile strength but raise the yield point by about 5000 lb./in.² in structural steel. Silicon treated steels are also less subject to hot straining and strain-aging embrittlement. Addition of 0.15 to 0.35% silicon produces fully killed steels which are sounder and cleaner and have proportionately improved mechanical properties. This is the range of silicon content usually present in high quality carbon and low-alloy steels of the forging and automotive types. Silicon contents between 0.50% and 1.00% are used in some high strength structural steels in order to take advantage of the higher yield point, tensile strength, fatigue ratio, toughness and resistance to air hardening conferred by silicon as an alloying element. Within this silicon range the steels are less subject to intergranular oxidation and have good rolling characteristics. The steels containing over 1% silicon and somewhat less than 2% tend to be prone to surface defects and are seldom used for structural purposes although appreciable amounts are used in cast and rolled forms for electrical machinery. Good rolling and surface characteristics are restored at 2% silicon. These steels are characterized by a high fatigue limit and are primarily used in springs. The higher electrical resistance and lower magnetic hysteresis loss of higher silicon steels reach optimum values at about 6.5% silicon, but there is little commercial application of steels containing more than 5% silicon. The high silicon electrical steels are usually quite low in carbon and manganese and are likely to contain "alumina" rather than "silica" inclusions.

PHYSICAL PROPERTIES

Arc welds in two steels: 0.12 C, 1.00 Si, and 0.2 C, 0.8 Si had the same yield and tensile strengths as base metal, namely 51,000 and 77,000 lb./in.², respectively, according to Hochheim.[1] The bend angle was 32.5 to 84°; unwelded had nearly 180°. Some of the tensile failures occurred in the weld, some outside. The specimens were 1.61 inches wide and 0.43 or 0.79 inch thick cut from X welds (no details). Weinman's[2] results with atomic hydrogen welding and a silicon steel rod are summarized in Table 1. The grain size of the deposit was much larger than that of similar deposits made with a 3% Ni or Cr-V (0.94 Cr, 0.18 V) rod, but the mechanical properties were not inferior. Fatigue tests were made by Taylor and Jones[3] with disappointing results (see review of literature on Fatigue Strength of Welded Joints) on rolled I beams with arc-welded reinforcing plates on the flanges. Both beams and plates were made of "silicon" steel containing 0.27–0.29 C, 1.0 Mn, 0.25 Si.

Table 1—Mechanical Properties of Atomic Hydrogen Welds. Weinman[2]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches	Reduction of Area, %	Rotating Bend Fatigue Limit, Lb./In.²
All-Weld-Metal	42,000	67,000	15.5	33	28,800
Single V weld in steel containing 0.09 C, 0.50 Mn	24,500*	63,900	17.5	61	20,000
Single V weld in steel containing 0.35 C, 0.60 Mo, 2.25 Ni, 0.40 Mo	35,000*	105,650	3.2	13	25,000

* = Proportional limit.

	C	Mn	Si	S	Rockwell B Hardness Base Metal	Heat-Affected Zone
Filler rod	0.47	0.56	1.98	0.040	...	..
All-Weld-Metal	0.34	0.55	1.98	0.040	...	..
Weld in 0.09 C steel	0.18	0.54	0.87	0.025	70	80*
Weld in Ni–Mo steel	0.14	0.55	1.59	0.013	108	116†

* = Weld-metal = 95.
† = Weld-metal = 106.

Drop tests on truncated cones ($^1/_4$-inch apex, 1-inch base) of silicon steel arc weld-metal were part of an extensive program of welding research carried out by the AERA in 1930–1931, Table 2. Clearly the Brinell hardness and resistance to compression depend primarily on the carbon content. The weld-metal was pearlitic, with the amount of ferrite varying with the carbon content.

Table 2—Drop Tests on Arc Weld-Metal. Salsich[4] and McKinney[5]

Specimen	C	Si	Mn	P	S	Brinell Hardness As-Welded	Brinell Hardness After 5 Blows	Brinell Hardness After 200 Blows	Percentage Compression 5 Blows	Percentage Compression 200 Blows
Electrode	0.615	2.03	0.86	0.022	0.028	.....	...	...	.....	.....
Deposit, bare	0.226	1.80	0.69	0.023	0.033	140–170	...	...	32.1–36.9	48.8–53.2
Deposit, coated	0.211	1.82	0.69	0.024	0.033	166–170	268	289	32.7–36.5	48.6–51.8
Electrode	1.219	2.21	0.98	0.023	0.012	.....	...	...	.....	.....
Deposit, bare	0.793	2.01	0.70	0.020	0.013	283	336	369	16.5	31.6

Table 3—Magnetic Properties of Silicon Steel Weld-Metal. Hess and Ringer[6,7]

Method of Welding	Silicon, %	Maximum Permeability As-Welded	Maximum Permeability Annealed	Hysteresis Loss, Watt Sec./Lb./Cycle As-Welded	Hysteresis Loss, Watt Sec./Lb./Cycle Annealed	Hysteresis Loss, Watt Sec./Lb./Cycle Hydrogenized
Oxyacetylene	0.46	2060 (7000)	3520 (7100)	0.0272	0.0180	....
Oxyacetylene	1.90	2100 (5600)	8225 (7800)	0.0234	0.0097	....
Atomic hydrogen	2	.....	.....	0.0223	0.0124	....
Atomic hydrogen	3.96	1800 (4000)	5500 (8000)*	0.0224	0.0112	0.0061
Oxyacetylene	4.35	1940 (4000)	3650 (4200)	0.0175	0.0088	....

* Hydrogenized specimens had a maximum permeability of 10,300 at 7000 gauss. Values in parentheses are gauss at maximum permeability.

Table 4—Recovery of Silicon from Oxyacetylene Welding Rods

Method of Welding	Carbon, % Rod	Carbon, % Deposit	Silicon, % Rod	Silicon, % Deposit	Manganese, % Rod	Manganese, % Deposit	Investigator
"Activated;" 20–30% excess oxygen	0.25	0.13	0.40	0.12	0.72	0.39	Keel[10]
No details; neutral flame	0.20	0.18	0.55	0.25	0.80	0.48	Dawson[11]
Neutral flame, rods contained 0.05–0.12 C, 0.15–0.25 Mn;	..	..	1.5	0.46	..	..	Hess and Ringer[7]
rods were 1/4 inch diameter; deposits were 3 inches diameter, 1 inch high, built up in successive 1/8-inch layers	..	..	2.5	1.90	..	..	Hess and Ringer[7]
	..	..	4.5	4.35	..	..	Hess and Ringer[7]
Weld in 0.39-inch steel containing 0.09 C, 3.8 Si	0.09	..	3.8	3.3	..	..	Portevin and Leroy[12]

The magnetic properties of silicon steel weld-metal in Hess and Ringer's[6,7] tests approached those of unwelded silicon steel. Atomic hydrogen and oxyacetylene weld-metal had about the same magnetic properties, Table 3. Annealing at 1000° C. for 2 hr. was essential to develop best magnetic properties. Heating in moist hydrogen for 20 hr. at 1100° C. reduced the hysteresis loss still further. Okada[8] stated that the increase of hysteresis loss in arc weld-metal on aging is reduced to nil by the addition of silicon and manganese (no details). The presence of manganese prevented the formation of large globules, which hindered arc welding with a silicon-coated electrode.[9]

RECOVERY AND PICK UP

Some results of the recovery of silicon from oxyacetylene welding rods are presented in Table 4. Without supplying details, Portevin and Leroy[12] stated that 1.5 to 2.5% Si practically prevents loss of carbon in "gas" welding commercial steels 0.39 inch thick of the same composition as the rod. In beads deposited on the surface there was a loss of 0.08 Si if the rod contained up to 0.5 Si, but only 0.01 Si if the rod contained 1.6 Si. A silica film was believed to form and prevent further loss of silicon. By melting a high silicon steel (presumably 2% Si) in an oxidizing flame it was possible to obtain a loss of 0.3 Si, no silica being found by analysis of the weld-metal. In welds there was a loss of 0.05–0.15 Si with steels containing up to 2.3 Si. Silicon did not affect the recovery of manganese.

Almost all the loss of silicon in oxyacetylene welding with a neutral flame occurs at the end of the rod, Tichodeev[13] found. Wire 0.16 inch diameter made from high frequency furnace melts containing 0.07–0.48 C, 0.36–1.03 Mn, 0.20–0.54 Si had 60 to 70% lower silicon content in the melted tip. It was believed that silicon was lost through the action of FeO dissolved in the molten drop. In carbon arc welding in a hydrogen atmosphere 80 to 90% of the silicon content of the wire was lost. Bare electrodes deposited in air recovered no silicon whatever.

Tichodeev's results on arc welding were not confirmed by other investigators. Salsich and McKinney, Table 2, lost 60 to 70% of the silicon content of their bare electrodes. Pilarczyk[14] showed, Table 5, that the recovery of silicon increases as the carbon content is increased.

Table 5—Recovery of Silicon from Bare Electrodes. Pilarczyk[14]

Carbon Electrode	Carbon Deposit	Manganese Electrode	Manganese Deposit	Silicon Electrode	Silicon Deposit	Recovery of Silicon, %
0.22	0.02	0.47	0.15	0.04	0.01	25
0.21	0.02	0.41	0.19	0.11	0.02	18
0.20	0.03	0.40	0.15	0.26	0.05	19
0.22	0.05	1.04	0.43	0.28	0.03	11
0.34	0.03	0.39	0.19	0.44	0.04	9
0.55	0.13	0.53	0.36	0.08	0.03	38
0.55	0.17	0.47	0.32	0.16	0.06	38
0.55	0.17	0.47	0.36	0.29	0.12	41
0.54	0.17	0.60	0.32	0.50	0.19	38
0.96	0.49	0.19	0.16	0.19	0.10	53
0.61	0.28	0.72	0.37	0.50	0.22	44
0.88	0.55	0.29	0.21	0.30	0.22	73
0.88	0.46	0.30	0.21	0.45	0.28	62

The bare electrodes were 0.16 inch diameter and the same procedure was used in all tests. Manganese did not affect the recovery of silicon. By increasing the carbon content of the bare electrode from 0.1 to 1.0%, Csilléry and Péter[15] reduced the loss of silicon from 90% to about 25%. The electrodes contained 0.15 to 0.35 Si. Losana[16] found that the recovery of silicon was better from thick bare electrodes (0.24 inch diameter) than from thin (0.12 inch diameter). With 0.2 Si in the electrode, the deposit from the former contained 0.10 Si, from the latter 0.04 Si. If the electrode contained 0.10 Si or less, the deposit contained none. Regè's[17] bare electrode 0.16 inch diameter containing 0.10 C, 0.74 Mn, 0.15 Si deposited metal with only a trace of silicon. Coating the electrode with mixtures of graphite, ferromanganese, and ferrosilicon (up to 20% graphite, 41.8% Mn, 5.72% Si in coating, no details) did not greatly improve the silicon recovery which was 0.218 maximum.

Schuster[18] deposited bare rolled electrodes on steel of the same composition, Table 6. The recovery of silicon increased from 60% to 90% as the silicon was increased from 1 to 4%.

Table 6—Recovery of Silicon from Bare Electrodes. Schuster[18]

Si		C		Mn		
Electrode	Weld	Electrode	Weld	Electrode	Weld	Notes
1.22	0.72	0.11	...	0.26	0.19	Electrode positive
1.22	0.73	0.11	0.034	0.26	0.19	Electrode negative
1.46	0.86	0.073	0.066	0.30	0.24	Electrode negative
4.0	3.53	0.011	...	0.28	..	Electrode negative & positive

The effect of silicon on the recovery of manganese from coatings in the tests made by Losana and Jarach[19] is discussed in the section of Recovery in the review of literature on Welding Manganese Steels (American Welding Society Journal, **17**(8) Suppl., 5-11(1938)).

There is good recovery of silicon in atomic hydrogen welding, as shown by Weinman,[2] Table 1, and Hess,[6] Table 7. The alloys used by Hess were made from Armco iron and probably contained no carbon and manganese.

Table 7—Recovery of Silicon in Atomic Hydrogen Welding. Hess[6]

Percentage silicon in filler rod	1.84	2.61	3.49	4.24
Percentage silicon in deposit	1.62	2.47	3.47	3.96
Recovery of silicon, %	88	95	100	93

Results show that silicon recovery improves as the carbon and perhaps silicon content of the rod increases. Manganese has no effect. The effect of carbon may be in oxidizing preferentially to silicon, although in steel refining silicon oxidizes preferentially in the presence of carbon. Carbon reduces the melting point. The atomic hydrogen process is best from the recovery standpoint. It has been pointed out by W. A. Spindler (private communication, August 1938), that Batty (*Metals Handbook* 1936 Edn., 639) discussing the acid electric process for cast steel states that at the high arc temperatures, Si, Mn and C are quite certainly oxidized simultaneously. Stoughton (Metallurgy of Iron and Steel, Fourth Edn., 182 (1934)) also states that carbon can oxidize in preference to silicon, and even reduce silica, at high temperatures possible in the Bessemer process.

The pick up of silicon by metal deposited in one or two beads by low carbon unalloyed electrodes on a plate containing 0.1 C, 0.4 Mn, 3.85 Si was 1.0, 0.7 and 0.5% Si with bare electrodes at 175, 155 and 110 amp., respectively, according to Haardt.[20] In two layers the bare electrode deposit picked up 0.2 Si at 155 amps. With a covered electrode a single bead picked up 0.5 Si; a double bead contained none. Columnar diffusion crystals were observed at the junction of deposit with base metal.

FUSION WELDING PROPERTIES

A number of authorities have condemned silicon steels for welding, generally for no reason. Granjon[21] stated that silicon in steel lowers "weldability" and Lewenz[22] believes that as little as 0.1 Si may spoil the welding properties of a steel. In Pohl's[23] experience, silicon structural steel ("as is well known") has poor weldability. At least some of the belief in the poor welding properties of silicon steel is prejudice. For example, in 1927 Füchsel[24] found that gas and arc welding were not satisfactory for silicon structural steel, but since the steel contained 0.20–0.45 Si, 0.40 C, the carbon content supplies an adequate reason for the low (20°) bend angle of welded joints. Scharlibbe[25] found that a high-carbon steel containing 0.9–1.2 C, 1.2 Si, 1 Mn was almost impossible to gas or arc weld. Another steel whose poor weldability especially in thin sections may be attributed to high carbon content is Houdremont's[26] steel containing 0.45 C and 1.5 Si with 1.0 Mn, or 1 Si with 1.5 Cr. However, Stieler[27] stated that high silicon content in low-alloy structural steels favors cracking in fillet welds. On the other hand, Schaper[28] found that Freund steel (0.08–0.13 C, 0.67–1.55 Si, 0.42–0.56 Mn) welded in about the same way as a plain carbon steel having a tensile strength of 68,000 lb./in.^{2} Rapatz and Schütz[65] state that up to 0.7 Si has no effect on cracking of low-alloy steel during arc welding, which agrees with E. Helin's statement (private communication, Aug. 1938) that with 0.2 C or more the silicon content of an electrode should not exceed 0.20%, but with lower carbon the silicon content may be higher without danger of cracked fillet welds. Helin believes some of the cracking difficulty may be attributed to slag in the weld. The arc-welded silicon structural steel freight cars referred to by Bode[29] were successful. Earnshaw[66] had no trouble arc or oxyacetylene welding austenitic, corrosion-resisting steels containing 5 Si, 15–25 Ni with a rod of base metal.

Of the two welding difficulties: cracks and poor fusion, the latter is the main one in welding silicon steel. According to Müller,[30] the silicon content of aircraft steels has no effect on their sensitivity to cracking during welding. The difficulty with silicon steels (1.5 Si or more), Schimpke and Horn[31] state, is that the silicon oxidizes in the arc and makes the slag viscous. Yet tests by Llewellyn[32] and Leitner[33] indicated that silicon steel had desirable arc-welding properties. Llewellyn stated that mild steel with 0.06–0.08 Si had meta' arc welding qualities superior to steel containing no silicon. Leitner automatically deposited a single bead from a bare electrode (0.16 inch diameter, 150–170 amp., 17–20 volts, d.c., electrode negative, speed = 39.4 ft. per hr., 0.09 C, 0.47 Mn, trace Si, 0.009 P, 0.019 S, 0.027 O_2, 0.006 N_2) on plates 0.39 inch thick and 8 x 12 inches, containing 0.04–0.09 C, 0.26–0.48 Mn, 0.10–0.62 Si. Upon being sectioned along the axis, the beads showed porosity that decreased as the silicon content of the plate increased. Porosity was still present in a plate containing 0.44 Si, but was absent in a plate containing 0.62 Si.

Basing his argument on the misconception that silicon increases the solubility of gases in liquid steel, which are evolved on solidification, Stine[34] reached the conclusion that for best arc welding properties, the maximum silicon contents of 0.50 and 0.20 C steels should be 0.08 and 0.02 Si, respectively. It is well known that 0.1 Si is sufficient to decrease the evolution pressure of CO in mild steel below 1 atmosphere (0.2 Si is sufficient in 0.3 C steel) and to prevent CO porosity. In other words, FeO will react with Si rather than with C. There is no evidence that Si in fractional percentages has a significant effect on the solubility of other gases, such as nitrogen and hydrogen, in steel. Indeed, the failure of silicon to react with nitrogen may account for blow-holes in silicon-killed steel, in contrast with aluminum-killed steel. For welded pressure vessels the silicon content is usually limited to 0.25% max. in this country and Germany, and to a trace in France. Dorey[35] prefers the silicon content between 0.05 and 0.1% for welded steel pressure vessels.

Aside from silicon in base metal, silicon in the welding rod has given rise to considerable difficulty. Hess[6] was unable to deposit bare electrodes made of commercially pure iron containing 1.84 to 4.24 Si, because large drops frequently short circuited the arc (300 amps., no details). Alloys of the same analysis appeared to give no difficulty in oxyacetylene[7] and atomic hydrogen welding. Tichodeev[36] found that low-carbon bare electrodes yielded an erratic arc if the silicon content exceeded 0.15% or the silica content exceeded 0.40%. (As J. R. Dawson points out (private communication, July 1938), the usual SiO_2 content of steel welding rods is about 0.010%, and of steel containing segregated non-metallic inclusions about 0.02%. The rods containing 0.40 SiO_2 mentioned by Tichodeev must have been wrought iron.) He concluded that bare electrodes should not contain over 0.1% Si. On the contrary, Paterson[37] believed that silicon was not disadvantageous in arc-welding electrodes if the manganese content was properly adjusted, and Swinden[38] recommended that silicon for killing in coated electrodes should be incorporated in the coating, as did Blomberg.[39]

For gas welding Swinden preferred a silicon-killed rod. Kinzel[40] referred to an effect of silicon on gas solubility (no details) but believed this disadvantage of silicon was offset by its fluxing action. "The major alloy improvement in oxyacetylene welding rods may well be considered the addition of 0.5 Si and 1.0 Mn." Quieter running metal is obtained if the steel is deoxidized with Mn in the furnace and subsequently with Si, than if the Si is used in the early stages of refining (no details). The silicon in the rod is oxidized preferentially to iron during welding, the manganese-silicon-oxygen slag being easier to manipulate by the oxyacetylene welder than slags rich in iron. A ratio of 3 parts Mn to 2 parts Si in the rod (0.80 Mn, 0.55 Si, 0.20 C), according to Dawson,[11] produces a slag of the correct viscosity and weld-metal that is cleansed by the deoxidizing elements in the rod. Fry[41] believed that silicon may be beneficial in fusion welding because it suppresses the formation of CO and prevents slagging of the steel. Tests in 1927 showed that 1% Si structural steel could be readily welded with a medium-manganese steel electrode (no details). The Mn prevented loss of silicon by vaporizing and forming a protective atmosphere, it was said. According to Hoffmann,[42] rods for plain carbon high-tensile structural steel should contain 0.15–0.25 Si as proof of being killed. A good filler rod[43] may contain 0.65 Si (for oxyacetylene, not for arc welding).

Silicon may be detrimental to the quality of oxyacetylene welding rods unless other elements in the rod are correctly proportioned. For instance, Kautny[44] observed that silicon increases the foaming of weld-metal and causes porosity. The porosity was said to be caused by oxidation of silicon (no details). In Streb's[45] experiments, spatter loss from oxyacetylene welding rods (0.025–0.085 C) did not depend on welding technique or composition of rod, although rods with 0.1 Si lost less by spatter than rods free from silicon (no details). A rod containing 0.6% Si, of which 0.4% was in the form of SiO_2 (probably a wrought iron rod), had no spatter loss whatever, but the weld-metal was porous, presumably because the slag had incorrect viscosity. Silica and hydrogen were said not to react at oxyacetylene welding temperatures. Consequently, the slag is made viscous by silica and does not permit the gas evolved in the melt to rise through the slag. That a slag of high viscosity may increase porosity was shown by welding very slowly with high-silicon rod. The weld contained no porosity because the gas had time to rise. Streb concluded that the SiO_2 content of oxyacetylene welding rods should be as low as possible but the silicon content should be 0.1 to 0.2% to indicate killing. He also found[46] that silicon hydride in the acetylene becomes SiO_2 or $FeSiO_3$ in the welding depending on the temperature and whether FeO is present.

Rods containing high silicon cause porosity in oxyacetylene welds in medium carbon steels, according to Holler and Frankenbusch.[47] The slag has a high viscosity and the deposit is red short and can be peened safely only at high temperatures. In plain-carbon steels the silicon content should be below 0.5% for good oxyacetylene welding. Zeyen[48] believed that a low-silicon content was permissible in oxyacetylene welding rods, but electrodes should contain none. In 1922 Diegel[49] stated that oxyacetylene welds should have not over 0.02 Si for best ductility. He found electrolytic iron (0.04 C, trace Si, 0.03 Mn) to be less fluid in welding than mild steel, and believed that 0.12 Si was not a disadvantage in oxyacetylene rods containing 0.08 C, if 0.45 Mn was present. The higher the manganese content, the higher might be the silicon, the ratio being 3 to 4 parts Mn to 1 part Si.

Filler rods containing rather high silicon are good for atomic hydrogen welding, according to Blomberg.[39]

Some of the apparent inconsistencies in test results on welding silicon steels will vanish when it is recalled that the term "silicon steel" refers to no less than three types of steels, as pointed out by F. T. Llewellyn (private communication, June 1938):

1. The steel containing not over 0.45 Si and 1.00 Mn, mistakenly called silicon steel by some structural engineers.
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3. Electrical silicon steel containing 1 to 5 Si (see section on Silicon in Steel).

RESISTANCE WELDING

Flash welds in bars 0.47 to 0.79 inch thick containing 0.15–0.18 C, 0.56–1.06 Mn, 0.00–0.35 Si (in seven steps) were quite satisfactory in Grahl's[50] tests. No details of welding are given. Machined flash welds annealed 1 hr. at 860° C. had 97 to 100% of the tensile strength of unwelded base metal, the lowest result being obtained at the highest silicon content. The elongation was uniformly divided between plate and weld and the reduction of area was 15 to 30%. The cold bend angle was 72° with no silicon and 39° at 1.15 Si (span = 4.7 inches, plunger thickness = 1.6 inches). The notch impact value was low at −15° C., Table 8. The specimen was 4 x 0.39 x 0.32 inch; the cross section of fracture was 0.39 x 0.20 inch, and the notch angle was 45°. A completely

ductile fracture was observed in the unwelded samples as low as +50° C., but in the flash welded only at +200° C. The annealed welds had a fine grain size and were free from oxide inclusions. Annealing at 1050° C. for 1 hr. did not improve the welds. It was Grahl's contention that silicon steels must be rapidly welded in order to exclude oxygen from the welds. His flash welding experiments supported the belief.

Table 8—Notch Impact Value (Mkg./Cm².,) of 1.10% Silicon Steel Annealed 1 Hr. at 860° C. Grahl[50]

Temperature of Test, °C.	−15	+50	+300
Unwelded	5 to 6.4	9.9	8
Flash Welded	1.2	4 to 8.6	9.0 to 9.8

Iron containing 4.65 Si was resistance butt welded by Burgess and Aston[51] in 1909. Freund steel (0.1 C, up to 1.55 Si, 0.5 Mn) and steel containing 1 C, 1.2 Si, 1 Mn are easy to resistance weld, according to Schaper[28] and Scharlibbe,[25] respectively. For spot welding, however, the silicon content of mild steel should be as low as possible (0.25 Si or less) in Hughes[52] opinion.

FORGE WELDING

Silicon steels (about 1% Si) are not suited for forge welding. The reason, to quote Grahl,[50] is that silicon favors the oxidation of iron at high temperatures. He made scarf welds in plates 0.47 to 0.79 inch thick and 2 inches wide, the length of the scarf being 2⅜ inches. The plates contained 0.15–0.18 C, 0.56–1.06 Mn, 0.00–1.35 Si (in seven steps) and were made from a melt to which ferrosilicon (80% Si) was added in the ladle. Welds were made both with and without a flux, which consisted of 35% ferrous reducing agent, 45% pure flux, 20% carbon. The flux did not improve the tensile strength of the welds in 1.15 Si steel more than 2500 lb./in.², which was of no significance. Heating the scarves in an electric furnace produced better welds than heating in a forge fire. The ratio of tensile strength of the unwelded to welded specimens (forge fire) fell from 87 to 60% as the silicon content increased from 0 to 1.35%, the elongation being 3% or less if the silicon content was 0.62% or greater. The ratio for an electrically heated weld in 1.15% Si steel was 80.1% (tensile strength of weld was 63,500 lb./in.², 7.2% elongation). The forge welds were cooled in still air and were not annealed. Specimens bent hot immediately after welding did not break unless the silicon content exceeded 1.07%. The cold bend angle was only 16° in a weld containing 1.15 Si.

Obviously, the controlling factor is rapidity of heating to welding temperature if the welding is done in an oxidizing atmosphere. As the silicon content increased, the oxide inclusions (two-phase) increased. The welded zone of a steel containing 1.15 Si contained only 0.71–0.94 Si uncombined. The more rapid the welding, the less opportunity there is for oxidation. In the electrically heated welds the portions of the weld near the surface were oxidized as in the forge fire welds. The inner third was free from oxides, whereas the forge fire welds, which were held a longer time at welding temperature, were oxidized throughout. The best welding temperature for steel containing about 1% Si was estimated to be 1435° C. (optical pyrometer), which was also the sparkling temperature. Sweating, caused by melting of FeO, commenced at 1370° C.

In Hahn's[53] tests with steels made from a charge containing 0.1 C, 0.04 P, 0.04 S, 0.15 Cu, 0.004 to 0.46 Si, bars 0.71–0.79 inch diameter forge welded successfully if the silicon content did not exceed 0.30%. Above 0.30 Si the steel welded but the welds were extremely brittle and cracked. Scharlibbe[25] could not forge weld a steel containing 0.9–1.2 C, 1.2 Si, 1 Mn.

Howe[54] found that the forge welding of mild steel is not affected by silicon (no details), and Burgess and Aston[51] successfully forge welded iron containing 4.65 Si. In 1870 Mrázek[55] found that iron containing 5.37 or 7.42 Si (trace of carbon) was easy to forge weld. A steel containing 0.155 C, 3.93 Si, 1.24 Mn was equally easy to weld, a steel with 0.474 C, 0.58 Si, 0.26 Mn, 0.13 Cu, was not so easy, and a high-carbon steel with 1.51 C, 0.17 Si, 0.24 Mn was too brittle to withstand the blows of a hammer. Jüptner[56] believed that uncombined silicon had a bad effect on forge welding. If the silicon was oxidized to SiO_2 to form a slag, it exerted a good effect. Such was not Schäfer's[57] experience, who admitted that sand was a good forge welding flux but condemned the forge welding of steels containing over 0.2 Si. Opposite opinions have been expressed[58] on the question whether silica in steel is good for forge weldability. Speller[59] stated that iron and steel for forge welding should contain less than 0.05 Si.

In water gas welding 0.2 Si definitely dooms the steel to failure, according to Diegel,[58] if the steel contains 0.10–0.15 C, 0.05 P, 0.05 S, 0.10 Cu and only 0.40–0.75 Mn. However, the silicon content may be 0.15 or 0.25% if the manganese content is 0.90 or 1.00%, respectively. Diegel believed that hammer slag (melting point = 1300–1320° C.) .s forced out of the joint in the liquid state, but did not state in what form the silicon existed in the slag. Pohl[60] also believed that Mn counteracts to some extent the bad effect of silicon on water gas welding of mild steel. With 0.4–0.8 Mn, the silicon content should not

Table 9—Hardness of Flame Cut Spring Steel. Matting[64]

Kind of Cut	Composition of Steel C	Si	Mn	P	S	Condition of Steel Before Being Cut	Rockwell Hardness of Uncut* Steel	Rockwell Hardness of Steel at x Inch Back of the Cut 0.04	0.08	0.12	0.20	0.32	0.39	Depth of Heat-Affected Zones Inch Preheat†	Cut‡
Hand	0.50	1.82	0.56	0.01	0.01	Unhardened	98.5B	93–97B	94–98B	98–99B	93–95B	95–96B	98–99B	0.12	0.032
Machine	0.54	2.03	0.60	0.01	0.01	Unhardened	99B	99–100B	97–100B	97–99B	97–98B	96–98B	98–99B	0.08	0.024
Hand	0.52	1.86	0.70	0.01	0.02	Hardened	41C	38–38.5C	42.5C	43–44C	44C	43.5–44C	43–43.5C	0.10	0.055
Machine	0.52	1.83	0.68	0.01	0.01	Hardened	40.5C	25–26C	28–31C	39C	41C	42C	42–43C	0.07	0.020

* Converted from Brinell (5 mm. ball, 750 kg. load, 30 sec.).
† Due to preheat flame; torch side of cut.
‡ Middle of thickness.

exceed 0.15%. According to Bauer,[61] silicon always has a bad effect on the water gas welding of boiler plate. Yet Weinrich[62] stated in 1937 that a boiler steel with 0.8–1.0 Mn, 0.25–0.30 Si was perfectly weldable by the water gas process.

FLAME CUTTING

Wiss[63] was able to oxyacetylene flame cut transformer iron with 4% Si and very low carbon, but the operation was slower than with mild steel. There was no change in the magnetic properties or structure at the edge of cut made through a packet of 300 sheets each 0.0138 inch thick and coated with varnish. If, however, 0.2 C was present with 4 Si, the steel could not be flame cut. High silicon iron (15 Si, 0.5 C) and silicon itself could not be flame cut with or without preheat.

The hardness of flame cut spring steel, Table 9, was practically the same as unaffected base metal in Matting's[64] tests. The hardness measurements were made on the torch side of the cut. Machine cutting hardened the unhardened steel and softened the hardened steel to greater extents than hand cutting, and created smaller heat-affected zones. Examination of the ground surface of the cut with a microscope (60×) revealed no cracks.

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SUGGESTED RESEARCH PROBLEMS

1. There is an obvious paucity of mechanical test results on welded silicon steels (many gaps in information on fatigue, hardness and impact value). Studies could profitably be made of the effect of silicon content on welding the new low-alloy high-strength steels, as well as other alloy structural steels. The effect of silicon in the plain carbon steels would form the basis of these more complicated studies.

2. One of the obvious needs is a study of the interrelated effects of carbon and silicon. These results could then be combined with the proposed similar study with regard to manganese and carbon. Especially in gas welding there appears to be a definite relation between the silicon and manganese present and this same relation, in a different degree, is no doubt also present in arc welding.

3. According to a correspondent, it is surprising that good forge welds can be made in relatively low-silicon steels (up to 1.35% Si) as well as in high-silicon steels (4 to 4.65% Si) whereas steels with intermediate silicon content cannot be forge welded, as shown in the section on forge welding. Research on the scaling of silicon steels with particular reference to the tenacity and melting point of the scale and its fluxing by welding fluxes might shed light on the confused subject of the effect of silicon on forge and spot-welding silicon steels.

4. The rôle of silicon in reducing porosity in arc and gas welds is not clearly defined. Leitner's[33] tests appear to be in the right direction but information on the composition and gas content of base metal and weld-metal in tests of the type Leitner made would be informative. The effect of welding speed and silicon content on the elimination of porosity should also be studied as well as the relationship, if any, between porosity, slag inclusions, and cracks in welded silicon steels.

5. The recovery of silicon from welding rods as a function of the composition of the slag and of the speed of welding.

IMPACT TESTS of WELDED JOINTS—II‡

A Review of the Literature from January 1, 1936 to January 1, 1938

By W. SPRARAGEN* and G. E. CLAUSSEN†

The report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION—WELDING RESEARCH COMMITTEE

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Summary

Introduction

One of the fundamental principles of materials testing is that the tests to be performed on a material for a given service should simulate the service as closely as possible. Since welds, like any other structural elements, often must withstand impact or suddenly applied loads, it is not unnatural that impact tests often are used.

Several European countries require impact tests of welded specimens in codes and specifications. This requirement is quite limited as yet in the United States.

Although nearly all these specifications require a notched-bar specimen in impact bending, there are considerable variations among the specifications in two principal respects:

(1) Type of specimen, Izod or Charpy (Mesnager)
(2) Specimens prepared from an actual welded joint or from all-weld-metal.

Since it appears unlikely that an internationally standardized notch impact specimen even for unwelded metal will be adopted for some time, the existing differences between specifications may be expected to remain indefinitely.

Tensile Impact Tests

The desirability for testing all zones of a single welded joint in impact has led to the use of the tensile impact test for welds. The elastic stress produced by tensile impact depends on the kinetic energy of the load, and on the volume, not the cross section, withstanding the blow. Up to the present the tensile impact test has been applied to welds only at the relatively low velocities available in pendulum machines. The reviewers have been unsuccessful in an attempt to correlate tensile impact value of welds with cross-sectional area or volume of welded specimens.

The Netherlands Welding Standards specify an all-weld-metal tensile impact test for qualification of welding rods for steel bridges. The threaded specimen is 0.35 inch diameter, 2.36 inches parallel section and must yield 80 mkg./cm.^{2} (366 ft.-lb. total) fracture energy with 26% elongation in 2.36 inches, 40% reduction of area when tested in a 540 ft.-lb. pendulum machine. Under some circumstances a "Charpy tensile bar" is included in the U. S. Navy process approval test plates.

I—NOTCH IMPACT TESTS

The variation of notch impact value with the dimensions of specimens is much more complex than the simple beam formula would lead us to expect. Although it is well known that for a fixed form of notch and fixed total depth of specimen the impact value is roughly proportional to depth back of notch in brittle materials and to the square of the depth in tough materials, the value usually lies between these two limits.

The Izod test does two things: (1) for comparatively brittle materials it gives a guide to the resistance to failure at a discontinuity; (2) it gives a guide to the resistance of a material during extension of a crack. This is probably true to some extent of the Charpy test as well.

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The Izod test is particularly important in detecting overheated structure in medium carbon steels, and revealing laminations and improper heat treatment.

The relationship between a number of different notch impact specimens for welds was found in an approximate manner at room temperature to be:

Charpy 8 mkg./cm.²	depth of notch				= 5 mm. (notch in side of weld)
Izod 8.5	"	"	"	"	= 2 mm.
Swiss 8.5	"	"	"	"	= 2.5 mm. (diameter of notch = 2 mm.)
DVMR 10	"	"	"	"	= 3 mm.
Mesnager 11.0	"	"	"	"	= 3 mm.

The same weld was used for all tests.

Different notch impact machines of the same type may yield different impact results on identical specimens if the mass of the pendulums is different.

There is one variable at any rate in the impact test whose variation does not have much effect on impact value. The distance between supports may be varied without appreciable effect on impact value. If, however, the supports are brought too close together, the results are complicated by the phenomenon of shear.

Location of Notch

The notch has also been located variously with respect to center line of plate or seam; e.g., at the transition between parent metal and weld, in the overheated zone.

The Izod value of all-weld-metal appears to be about 50% greater when the notch is parallel to the direction of deposition than when it is perpendicular. The notched specimen is particularly useful for detecting the presence of embrittling metallographic constituents, such as nitrides, that are deposited during aging or in slow cooling after annealing, and for locating the temperature of the zone of transition; it does not reflect the tensile properties of weld or parent metal.

The unnotched specimen may give lower impact values than the notched if the notch extends beyond the depth of a coarsely crystallized zone. Primarily, the unnotched specimen gives information about the quality of surface and junction zone, as well as about the degree of penetration.

There are still those who are skeptical about welding and regard the heat-affected zone as necessarily brittle in any steel. Tests have shown the zone to have ample low-temperature Charpy value even in base metal that is brittle in the Charpy test at low temperatures. Exhaustive Izod tests on as-welded and annealed joints in mild steel and in cast steels with 0.10 to 0.30 C showed that the heat-affected zone was always superior to base metal.

If anything, the heat-affected zone in stress-relieved (650° C.) arc welds in normalized (820° C.) plates $1^3/_8$ inches thick containing 0.25 C., $2^1/_4$ Ni had higher Charpy value than base metal.

Standard Weld Tests

The French Welding Institute's standard tests for filler metal have been extended to include both oxyacetylene and covered electrodes. A new German Tentative Standard Method for Notch Testing of Butt Welds (DVM A122, April 1936) stresses the importance of placing the notch in the fusion zone. The new German Boiler Code specifies the DVMR specimen for plate less than 0.47 inch thick and the VGB specimen for thicker plate. The Mesnager specimen with notch in side of weld is specified in the Czechoslovakian Standards (CSN 1120). The Portuguese Navy requires minimum Charpy values for welds of high and low strength. The Italian Standard for Gas and Arc Welding in Naval Construction requires that the average of four Mesnager specimens from a V butt weld attain 5 mkg./cm.² In this country the A. S. M. E. Rules for Containers for Gases and Liquids at Sub-Zero Temperatures down to −150° F. require a Charpy or Izod specimen, both with Charpy keyhole notch. A Charpy specimen (2 mm. drilled hole, A. S. M.) is included in the process approval test plates by the Navy for welding carbon-molybdenum and related steels. The notch is placed in weld-metal, base metal and fusion line on both sides of the plate. The American Bureau of Shipping requires an Izod test specimen identical with Lloyds for qualifying weld-metal deposited by a new process.

Correlations with Other Tests

In order to determine what properties the weld impact test reveals, several attempts have been made to correlate the impact value of welds or weld deposits with the results of tensile, bend, hardness, fatigue, X-ray and service tests on identical materials. As in unwelded material, there appears to be no relation between yield point, tensile strength, yield ratio, energy absorption and hardness, and the results of impact tests. Between impact value and elongation, as well as fatigue limit, there is proportionality in general but the relation is by no means exact and sometimes fails to hold.

II—RESULTS OF IMPACT TESTS OF WELDS

Carbon Content

As the carbon content of weld-metal increases, the notch impact value decreases. The DVMR value (notch in root) of welds made with heavy covered electrodes decreased from 12.4 to 5.6 mkg./cm.² as the carbon content of base metal increased from 0.11 to 0.68%. Although the weld-metal was not analyzed, doubtless it picked up carbon to an extent depending on base metal. The DVMR value of base metal decreased from 17.0 to 5.3 mkg./cm.² in the same carbon range and oxyacetylene welds made with an alloy steel rod decreased from 9.8 to 2.0 mkg./cm.² The impact value of welds rapidly decreased as the carbon content increased up to 0.3% but decreased much less rapidly thereafter up to 0.7%. An increase in carbon content of base metal also increases the ability of the steel to harden and become brittle in the heat-affected zone.

Using a Mesnager specimen with notch parallel to and in the heat-affected zone created by depositing one or four beads of an electrode at 180 amp. 7 inches per minute on plates 0.59 inch thick containing 0.19 to 0.67 C, one investigator found that unaffected base metal decreased from 25 to 11 mkg./cm.² as carbon content increased. Single layer deposits lowered the impact value to 16 at 0.19 C, and nil at 0.67 C, whereas four layers did not lower the notch impact value to any extent.

The weld and heat-affected zone in arc-welded cast steels containing 0.10 to 0.30 C, 0.4–0.5 Mn, 0.2–0.3 Si had higher Izod value than the unwelded casting. Stress annealing at 650° C. and annealing at 900° C. were always beneficial, the Izod value being increased as much as 15 ft.-lb. in most instances.

Oxyacetylene welds were made in steels with less than 0.10 C up to 0.33–0.37 C, 0.39–0.59 inch thick. Notch impact tests with the DVMR specimen indicated that the notch impact value as-welded depended on the quality of the rod, not on the carbon content of base metal. In all

welds a reverse layer in the root was beneficial. Forging and peening also increased the notch impact value, particularly with low-grade rods.

Regardless of location of notch the impact value in most of the rail joints in arc and gas welds was higher than, or at most slightly less than, the unwelded rail. There was no relation between notch impact value and ductility in the static tensile test.

Low-Alloy Steels

In welded Ni-Cr steel (0.29 C, 0.26 Mn, 3.25 Ni, 1.35 Cr, water quenched from above Ac_3, tempered at 580° C.), the base metal was 60 Izod, heat-affected zone was 30 ft.-lb. Izod without preheat.

Notching of the heat-affected zone beneath a bead of arc deposited weld-metal was adopted to study steels containing 0.14–0.24 C, 0.4–1.3 Mn, with fractional percentages of Cu, Cr or Mo. It was found that the Mesnager value after four beads had been deposited was higher than unwelded base metal. A single bead lowered the impact value 2 or 3 mkg./cm.^{2}, which was not important. Welding velocity or any other index to the maximum rate of cooling in the vicinity of the transformation ranges of the steel under consideration is an important factor in assessing the results.

The heat-affected zone in arc-welded steel containing 0.17 C, 2 Ni, 1 Cu had nearly twice as high notch impact value (V notch Charpy, no details) as base metal. Oxyacetylene butt welds in a steel 0.39 inch thick containing 0.18 C, 0.60 Si, 1.3 Mn, 0.40 Cu made with a low-carbon rod having some Ni, Mn and Cu, had 7.4–8.3 mkg./cm.^{2} DVMR compared with 6.9–7.6 mkg./cm.^{2} for unwelded base metal. A reverse bead raised the DVMR value to 8.7–9.6 mkg./cm.^{2} Peening had less effect than the reverse run.

In an oxyacetylene weld containing 0.11 C, 0.7 Mn, 0.7 Ni annealing and hot peening raised the DVMR value from 5–9 to 9–12 mkg./cm.^{2} The DVMR value of an oxyacetylene weld containing 0.10 C, 0.7 Cr, 0.4 Mo, 0.4 Si, 0.5 Mn was 10–22 mkg./cm.^{2}

In the Low-Alloy Steel Committee's presentation of the properties of gas and arc-welded steels a great many determinations of Charpy value are given. The values range from 20 to 65 ft.-lb. It is impossible to summarize the results of all the low-alloy steel tests. The reader interested in some special alloy steel or heat treatment is referred to the Low-Alloy Steel Committee's report, to this review and the previous review published in the April 1936 Welding Journal. Suffice to say that many of these alloy steels and welds therein are particularly suited to withstand severe impact stresses.

Austenitic Steels

Austenitic filler metal is sometimes used for welding mild steel. Welds made with a mineral covered electrode containing 25 Cr, 12 Ni in aluminum treated S. A. E. X1020 had 70 ft.-lb. Izod at +20° C., 60 ft.-lb. at −120° C. Heat treatment (2 hr. 600° C., furnace cool, or $^1/_2$ hr. 850° C. air cool) had no effect on different grades of austenitic weld-metal. The DVMR value of welds made with 18–8 electrodes in plain carbon steels with 0.11 to 0.68 C was 13 to 15 mkg./cm.^{2} independent of base metal.

Nitrogen (and Oxygen)

Perhaps the greatest utility of the impact test for welds lies in its infallible detection in mild or alloy steels of constituents, such as nitrides, carbides or oxides, that have been in some way deposited in a damaging form within the metal.

The impact test has been of most service to the welding industry in demonstrating the benefits to be derived from effectively coated welding rods. Tests by a large number of investigators conclusively demonstrate the superior impact resistance of heavily-coated electrodes of the shielded-arc type. The advance in Izod value from 5 ft.-lb. with bare wire in 1920 to 40 ft.-lb. easily obtained at present with any good quality electrode has been rapid and continuous. Unannealed gas welds generally have not more than 80% of the impact value of the parent metal, carbon arc welds (unprotected) approximately 20 to 40%. But values as high as 100% have been reported in heat-treated, automatic carbon-arc welds made in hydrogen.

Annealing

It was found that the impact value of arc welds prepared with bare or ineffectively coated electrodes was invariably decreased by 10 to 50% by annealing at about 1550–1700° F., whereas gas welds were improved by such treatment. Welds containing over 0.025 to 0.05% nitrogen or oxygen, introduced by bare or improperly coated electrodes, will display this peculiar effect, which may be ascribed to precipitation of oxides or acicular nitrides during slow cooling after annealing and is not usually found after rapid cooling as in normalizing.

The normalizing effect of the upper layers on the lower layers of bare wire welds is detrimental to impact properties. In gas-free welds the impact value after annealing is dependent on temperature and hence on grain size. Annealing temperatures above 900° C. coarsen the grain size in plate and weld in mild steel and, therefore, lower the impact resistance. In gas-free, high-strength welds, such as shielded arc welds, annealing just above Ac_3 may raise the impact value at 20° C. by 30%. One investigator found an Izod value of 3 ft.-lb. in the top run of a weld having 25 ft.-lb. in the lower layers.

In tests of 70° V butt welds made with blue asbestos covered electrodes in $^1/_2$ inch mild steel plates. The Izod values were 35 to 80 ft.-lb. at +100° C., 15 to 25 ft.-lb. at 500° C. Both annealing and normalizing improved the Izod value 20% at +100° C., but did not change the transition temperature or the Izod value at 500° C. The Izod value of normalized welds was unchanged by heating nearly 4 months at 250 or 450° C. with or without a tensile stress of 12,000 lb./in.^{2} The specimens were tested within 24 hr. of removal from the furnace. The Izod value at +20° C. dropped from 25 to 10 ft.-lb. after 45 days at room temperature following a water quench from 650° C. The weld contained 0.06 C, 0.65 Mn, 0.066 N_2.

The heat effect due to welding had a much more beneficial effect on the mild steel used by one investigator than annealing at 650, 800 or 900° C. Unwelded base metal had an Izod value of 25 ft.-lb., weld-metal was 72 ft.-lb. and the heat-affected zone was 45 to 68 ft.-lb. The annealing treatments raised base metal to 26–38 ft.-lb. but had no important effect on heat-affected zone or weld-metal.

In tests of 70° V oxyacetylene welds in mild steel (tensile strength = 68,000 lb./in.^{2}) for compressed gas tanks 0.59-inch wall thickness, normalizing 20 minutes at 900° C. raised the DVMR and VGB values from 6 to 14 mkg./cm.^{2}, despite the fact that the root of the weld had been ground out to a depth of 0.2 inch and reverse welded.

Widmannstätten structure was shown to be one of the chief causes of low notch impact value in welds. Oxyacetylene deposits (0.19 to 0.46 C, 0.3–0.9 Mn, up to 0.4 Si) in the as-welded condition with Widmannstätten structure had 10 ft.-lb. Charpy. A reverse run and an-

nealing at 850° C. raised the Charpy value to 25–35 and 55–65 ft.-lb., respectively.

Annealing at 900° C. raised the Mesnager value of good arc and gas welds; quenching from 900° C. reduced the Mesnager value only if the carbon content of the deposit was 0.26% instead of 0.13%, or if the electrode coating was of poor quality. The same was true of deposits quenched from 600° C. and held 40 days at room temperature.

Welding Procedure

Peening and also forging of a finished weld is beneficial to impact value; increases of 10 to 100% have been reported. As plate thickness is increased from $^3/_8$ to $^3/_4$ inch the impact value of all types of welds tends to decrease in comparison with parent metal; the increase in impact value produced by hot forging after welding is greater in the thinner plate. The effect of current strength on impact value, is relatively small although unusually high or low current lowers the impact value. The effect on impact resistance of A.C. as compared to D.C., of polarity and of arc length have also received attention but no effect of any magnitude was discovered. Within wide limits the quality of a flame cut bevel is without effect on the impact value of the joint. Backhand welding, because it gives rise to less severe overheating, is preferable to forehand from the standpoint of impact resistance of welds in steel.

Non-Ferrous Welds

Oxyacetylene welds in soft and hard-rolled aluminum (99.5% purity, 0.39 and 0.79 inch thick) made with aluminum rods of commercial purity had about one-half as high notch impact value (DVMR as well as VGB) as unwelded base metal. Cold hammering improved the notch impact value 1 to 4 mkg./cm.^{2}, and hot hammering raised the value to that of unwelded base metal.

Flame Cutting

The notch impact value of flame cut surfaces has been determined by three investigators who agree that, for the steels tested and with the specimens employed, flame cutting does not have an adverse effect. The steels were: $^1/_2$-inch plate containing 0.24 C and 0.46 Mn, $^3/_4$-inch plate containing 0.10 C, 0.44 Mn, or 0.18 C, 0.47 Mn; $^3/_8$-inch plate containing 0.12 C, 1.0 Mn, 0.6 Si, with either 0.3 Cu, or 0.4 Cu and 0.1 Mo.

Welds at Low and Elevated Temperatures

Tensile impact tests of machined resistance butt and atomic hydrogen welds in steel containing 0.25 C showed that the welds had about the same ductility but 20% lower fracture energy than base metal at all temperatures. Impact energy and ductility were about 20% lower at −80° C. than at +20° C. with unexplained variations at intermediate temperatures. Bronze-welded joints had one-half the fracture energy and ductility of unwelded 0.25 C steel. Had the bronze the same yield and tensile strengths as the steel (say 70,000 lb./in.^{2}, which is not difficult to attain with fluxes and rods now available), the deformation probably would not have been restricted to the relatively small proportion of bronze in the specimen, and the tensile impact value would have been higher. Oxyacetylene and arc-welded 18–8 had 50% lower fracture energy than unwelded 18–8 but 50% higher than plain carbon steel. The fracture energy was a little higher at −80 °C than at +20° C. with no difference in ductility. All fractures were fibrous at +20 °C and −80° C. The relation between static and impact tensile results at +20 to −80° C. was not at all close.

Welded $3^1/_2$ Ni, 0.20 C steel had nearly the same tensile fracture energy at −73° C. as at +20° C. On the other hand, welded $3^1/_2$ Ni, 0.35 C steel had only 10% of the room-temperature fracture energy at −46° C. Less pronounced decreases were found with welded plain carbon and 2% Ni steels.

Welds made with mineral or combustible covered electrodes in aluminum treated S. A. E. ×1020, $^1/_2$ inch thick had Izod values of 45 to 76 ft.-lb. at +20° C., 15 to 50 ft.-lb. at −78° C. and 4 to 31 ft.-lb. at −120° C. With notch in scarf the Izod was 80 ft.-lb. at −78° C. and 15 to 90 ft.-lb. at −120° C. Obviously the treatment with aluminum was beneficial to base metal. However, in plate $1^1/_2$ inches thick the scarf had only 3 to 10 ft.-lb. at −120° C. Stress relieving at 650° C. slightly raised the Izod of scarf at +20° and −50° C. but had no effect at −120° C. or on the weld. Normalizing at 900° C. was not advantageous.

Stress-relieved two-pass and three-pass D.C. arc weld-metal (1.6 Ni, 0.10 C) had 36 ft.-lb. Charpy at 20° C. and 22 ft.-lb. at −60° C., which was the same as fine grained rolled 0.09 C steel perpendicular to the direction of rolling or slightly below rolled 0.25 C, 2.4 Ni steel parallel to the direction of rolling. The heat-affected zone in base metal (2.33 Ni, 0.45 Mn, 0.14 C) had the same Charpy value (36 ft.-lb.) at −60° C. as unwelded base metal, whether or not stress relieved. Stress relief raised the Charpy value of weld-metal only 4 ft.-lb. at −60° C.

Investigations show that the high and low temperature impact characteristics of good welds are practically identical with those of parent metal. A minimum in impact value is usually found at 400–550° C. for welds in mild steel; for tough welds the minimum value is usually about 50% of the impact value at 20° C.

WELDED STRUCTURES

Welded structures of all sorts can be relied upon for excellent resistance to impact in service. By impact in service is meant not alone the repeated hammer blow received say by a welded rail joint from passing locomotives but also the unexpected sudden blow of unusual intensity under which the weld must hold to prevent widespread damage.

There is given in this and in the earlier review examples of welded joints which have withstood unusual impact loads as well or better than the base materials. These examples cover a wide range and are sufficient evidence that welds properly made can withstand impact blows. The behavior of large structures under impact cannot accurately be deduced from tests of small models in the ordinary impact machine. Under repeated heavy impact, welded beams are superior to riveted beams of equivalent static strength. In drop-testing welded demolition bombs the bombs themselves act as the ram. The maximum diameter of the projectile must penetrate at least one foot below the surface of the concrete. Bombs welded according to the A. S. M. E. Code for Class I pressure vessels successfully pass this test. Unrelieved shrinkage stresses in a welded seam undoubtedly lower its impact resistance, although there is no quantitative information on the subject.

Impact Tests of Welded Joints

INTRODUCTION

ONE of the fundamental principles of materials testing is that the tests to be performed on a material for a given service should simulate the service as closely as possible. Since welds, like any other structural elements, often must withstand impact or suddenly applied loads, it is not unnatural that impact tests often are used. The commonest impact test is the notch impact test in which a notched beam about $^3/_8$ inch square is struck by a swinging pendulum. It is a fact that the least important factor in the notch impact test, as it is customarily carried out, is the velocity of the blow. For example, Southwell[1] and Schuster[2] found that slow bend tests on notched specimens at room temperature reproduce the energy and character of fracture obtained in the notch impact test of specimens of identical shape. Sergeson and Poole[3] showed that the decrease in notch impact value to nearly zero of steel (0.35 C, 0.55 Mn) between +21° and −73° C. (+70° and −100° F.) was not reflected in impact tensile value (specimens $^1/_4$ inch diameter, 1 inch or 0.1 inch parallel section, 0.3 inch depth of notch), which remained the same throughout the range of temperatures. Therefore, the notch impact test is made the subject of a separate section in the present review. Impact tests more strictly so-called will be discussed first. The review is concluded with some recent examples of the service behavior of welds and welded structures under impact loads.

TENSILE IMPACT TESTS

The approximate design theory states that when an axial tensile load is suddenly applied to a bar, the extension is twice as great as that produced by a static load of equal static weight. More accurately, the elastic stress produced by impact depends on the kinetic energy of the load, and on the volume, not the cross section, withstanding the blow. Experiment and theory regarding axial impact; that is, load involving kinetic energy, are not well developed, but Arnold's[4] experiments on weights falling on rails show that impact stresses may be very much greater than predicted by approximate theories. The peak impact stresses, act, however, for exceedingly short intervals of time, and may not be so dangerous as their magnitudes suggest.

The kinetic energy or velocity of the blow is an important factor in tensile impact testing, as Jenks[5] has shown. Study of the velocity factor at Watertown Arsenal[6] has shown the falsity of the belief that brittle fracture is favored by increased velocity of loading. It was believed that since crystalline slip occupies a definite time, and since the stress at which flow starts appears to be raised as the velocity of strain increases, there would be no time for slip at high velocities of strain. On the contrary, the experiments at Watertown Arsenal on heat treated steel (0.37 C, 0.5 Mn) showed that as the velocity of loading in tensile impact increased beyond the critical velocity up to 310 feet per second (maximum investigated) the specimen fractured with increasing elongation and reduction of area, albeit the energy to fracture decreased. An interesting confirmation of the effect of kinetic energy is provided by the hammer blow tests made by Inglis[7] on sections of 5-inch tubing, 1-inch wall. If flattened in 3 blows the tubing did not crack, but some cracks appeared if the flattening was done in a single heavy blow.

Jenks pointed out that many factors in the tensile impact test remain to be elucidated. A formula of limited application relating size of specimen to tensile impact value has been evolved by Garcia.[8] Using a Charpy pendulum, Duwez[9] found that plots of tensile impact energy and elongation to fracture *vs.* length of parallel section (specimens 0.59 inch diameter, 0.39 to 4.3 inches parallel section, made of Cu, Al or steels with tensile strengths = 57,000 to 120,000 lb./in.^{2}) were straight lines. Extrapolated to zero length of parallel section the lines resulted in the determination of a "specific" elongation and "specific" energy which were independent of length and characteristic of each material. The effect of velocity was not studied.

Up to the present the tensile impact test has been applied to welds only at the relatively low velocities available in pendulum machines. Tensile impact tests of machined resistance butt and atomic hydrogen welds (specimens 0.80 inch parallel section, 0.20 inch diameter; velocity of blow about 12 ft./sec.) in steel containing 0.25 C were made by Henry[10] at +20 to −80° C. The weld had about the same ductility but 20% lower fracture energy than base metal at all temperatures. Impact energy and ductility were about 20% lower at −80° C. than at +20° C. with unexplained variations at intermediate temperatures. Bronze-welded joints had one-half the fracture energy and ductility of unwelded 0.25 C steel. Oxyacetylene and arc-welded 18–8 had 50% lower fracture energy than unwelded 18–8 but 50% higher than plain carbon steel. The fracture energy was a little higher at −80° C. than at +20° C. with no difference in ductility. All fractures were fibrous at +20° and −80° C. The relation between static and impact tensile results at +20 to −80° C. was not at all close. The expected connection[11] between the product of static tensile elongation and strength, and tensile impact value did not materialize, perhaps because the test specimens were of different sizes or because the elongation of the 2-inch machined tensile specimens was restricted by the weld.

Warner's[12] arc-welded, low-alloy steel tensile impact specimens for tests at room temperature were $^1/_2$ inch square, 1 inch parallel section; for tests at low temperatures the cross section was $^1/_2$ x $^1/_4$ inch (velocity not stated). In both types of specimens the weld was machined flush and the fillets were $^1/_8$ inch radius. The tensile impact ductility was not measured or mentioned. At 20° C. the welds had 10 to 20% lower fracture energy than unwelded base metal, but the variations in fracture energy among the eleven steels that were tested were much smaller than the variations in static tensile strength and ductility. The maximum hardness of the heat-affected zone was very approximately proportional to the tensile impact value. Welded $3^1/_2$ Ni, 0.20 C steel had nearly the same fracture energy at −73° C. as at +20° C. On the other hand, welded $3^1/_2$ Ni, 0.35 C steel had only 10% the room-temperature fracture energy at −46° C. Less pronounced decreases were found with welded plain carbon and 2% Ni steels. The small low-temperature specimen required one-half as much energy to fracture in tensile impact at room temperature as the larger specimen. The reviewers have been unsuccessful in an attempt to correlate tensile impact value with cross-sectional area or volume of welded specimens. The absence of correlation is believed to be accounted for by the non-

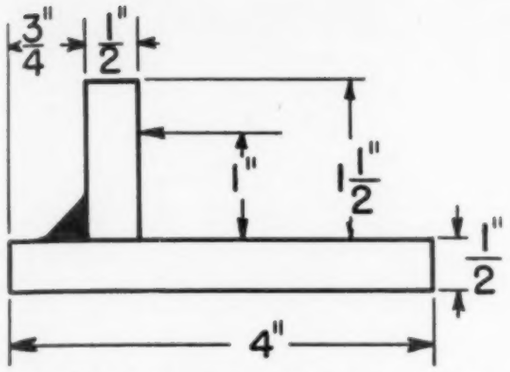

Fig. 1—Fillet Weld Impact Specimen Used by Vogel and Hess[25]

uniform distribution of strain in the necked region and by the variation of this non-uniform distribution with the cross-sectional shape and dimensions. If a full section of a weld is included in the parallel section of the tensile impact specimen, variations in width of weld and heat-affected zones as well as in microstructure throughout the joint further complicate the problem of correlation by providing opportunity for the development perhaps of two necks, and by creating local differences in yield and necking stresses.

Resistance butt and flash welds in plain carbon, low-alloy, and stainless steels were tested in tensile impact by Galibourg and Ballay,[13] who used a specimen 0.18 inch diameter, 1 3/8 inches parallel section, 0.04-inch fillets. The flash welds were superior to the resistance butt welds in energy absorbed and in elongation in nearly all instances. Best impact value and elongation were obtained in flash-welded stainless steel containing 20 Cr, 8 Ni, 0.20 C, as-welded or water-quenched from 1100° C. The energy absorbed was 120 ft.-lb.; the elongation was 33.5 to 39%. The highest values among the other steels were obtained with plain carbon steel (0.10 C), which absorbed 65 ft.-lb. at 27.5% elongation. Steels with 0.1 C, and 3 or 5% Ni, or with 13 Cr, 0.3 C were inferior, although they were tested only after heat treatment. There was no close relation between static and impact tensile properties, nor were the properties of the unwelded materials determined.

Contrary to available experimental results, Matting and Otte[14] stated that tensile impact tests of welds (no details) yield the same information as static tensile results. Wiegand[15] objected to the tensile impact test for welds because, of necessity, a small specimen generally is used and a single defect has a disastrous effect on the results. Yet the Netherlands Welding Standards[16] specify an all-weld-metal tensile impact test for qualification of welding rods for steel bridges. The threaded specimen is 0.35 inch diameter, 2.36 inches parallel section and must yield 80 mkg./cm.^{2} (366 ft.-lb. total) fracture energy with 26% elongation in 2.36 inches, 40% reduction of area when tested in a 540 ft.-lb. pendulum machine. Under some circumstances a "Charpy tensile bar" is included in the U. S. Navy process approval test plates.[17]

Suggesting research problems in the impact testing of welds, Henry[18] pointed out the desirability for determining the tensile impact value of a wide range of welded metals. His belief that determinations of the critical velocity of welds should be made may be a bit premature. Henry also suggested the application of other impact tests, such as the torsion impact test, to welds. Possibly Ellis's[19] single-blow drop test or Hruska's[20] elastic rebound hot hardness test for determining the hot hardness of materials subjected to shock at elevated temperatures may find some application to welds. Static notch tensile tests of welds have been reported by Hackert and Zeyen.[21] A 33% increase in yield strength was created by a notch 2 mm. deep, 1.5 mm. wide, 0.75 mm. radius, in a bar 18 mm. diameter, the yield strength being the stress at 0.2% elongation in 8 mm. at the notch.

CAVITATION

The accelerated cavitation tests in fresh water made by Mousson[22] on wrought iron plates covered with two layers of arc deposited plain carbon, low-alloy and stainless steels, as well as with oxyacetylene deposited bronzes and stellite, open up a new field of impact testing for weld-metal. Cavitation of metals is a form of mechanical destruction by hammer blows exerted through the collapse of air bubbles clinging to a rapidly moving surface, such as a propeller blade, in a liquid such as water. Turbine runners subject to cavitation, which appears as deep pits on the surface, often are resurfaced by welding. The weld deposits tested by Mousson behaved about the same as rolled material of the same composition, and lost only 5% as much by volume through cavitation after 16 hr. at 20° C. as sprayed deposits. Chromium irons were least attacked. Von Schwarz and Mantel[23] state that with materials having large energy of elastic deformation, destruction in cavitation tests occurs at flaws, which may be significant in welded surfaces. Cavitation was observed on the 18–8 surfaced turbine runners repaired by Hess[24] after 2 years of service, but Stabley observed none whatever on the 18–8 deposits of his cast iron runners after a similar period of service.

FILLET WELD IMPACT TEST

The impact test for fillet welds suggested by Vogel, Fig. 1, has been subjected to analysis by Hess.[25] Strips 1/2 inch thick are fillet welded at right angles with a fillet weld 1/2 inch long, 1/4 or 3/8 inch thick. Good welds fractured in the throat section. Fracture occurred elsewhere if there were laminations in base metal or slag along the fusion line. The time to fracture with different available pendulum energies varied inversely with the average velocity of the pendulum during fracture. However, the fracture energy did not vary with available pendulum energy and velocity. Although no numerical information was supplied, welds made with covered electrodes absorbed more energy and required a greater time to fracture than bare electrode welds. A strange notch impact test for fillet welds, Fig. 2, has been suggested by DuRietz.[26] A bead of weld-metal is deposited in a 90° V occupying the upper 5 mm. of plates 10 mm.

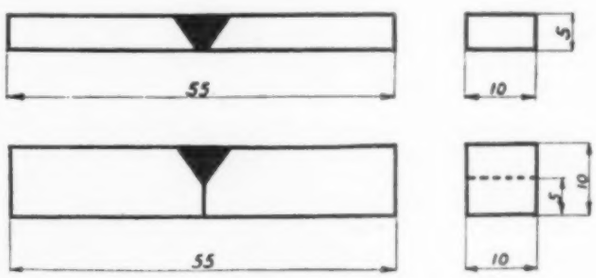

Fig. 2—Specimens Used by DuRietz[26]
Upper: unnotched specimen
Lower: fillet weld notch impact specimen. The notch is not machined.
Dimensions in millimeters

thick. The lower 5 mm. of separation acts as the notch in the impact test. No results were communicated.

NOTCH IMPACT TESTS

General

Before an attempt is made to show what purposes the notch impact test fulfills in welding, the significance of the test will first be discussed, following which the types of notch impact specimens in common and uncommon use will be surveyed. A thorough analysis of the characteristics of notch impact tests as revealed in the literature was made in 1936 by Hoyt,[27] and a short general outline was provided in the earlier review of literature on Impact Tests of Welded Joints (AMERICAN WELDING SOCIETY JOURNAL, **15** (4) 2–10 (1936)).

Among the most informative of recent notch impact studies is Schuster's,[2] who showed that slow bend tests of Izod specimens revealed the same fracture energy and type of fracture as Izod tests of the same material. Mailänder,[28] using VGB specimens of steels with up to 0.35 C, was unable to confirm the equivalence of static and impact bend tests, which was found by Schuster and Southwell for Izod and Oxford specimens. Usually the static fracture energy was higher than the impact fracture energy below +20° C. but lower above +20° C. In some tests there was a difference of 14 mkg./cm.^{2} in 26 mkg./cm.^{2} between the static and impact specimen of the same steel at the same temperature. Recording static bending load and movement parallel to the axis of the specimen, Schuster found that cracks started in tough and brittle steels of the same static tensile strength at the same bending load, which was slightly below the peak load attained. Consequently, Schuster regarded the term "notch brittle" as misleading since the Izod test does not distinguish between the energy required to start the crack and the entirely unrelated energy required to propagate the crack. Schuster found that the sensitive Izod notch includes proportionately more of the energy required to propagate the crack than may be desirable. The Izod test does two things: (1) for comparatively brittle materials it gives a guide to the resistance to failure at a discontinuity; (2) it gives a guide to the resistance of a material during extension of a crack. While an abnormally low Izod value is proof of poor qualities in both respects, a high Izod value may yet involve failure of one of the two factors. As Swinden[29] also pointed out, it is impossible to discriminate which of the two factors may have caused too low Izod value. What Schuster found is true for the Izod test is probably true to some extent of the Charpy test as well.

Like Schuster, Inglis[7] in his critical evaluation of impact tests believed that because a part is not subject to shock in service is no reason for the abandonment of the notch impact test in inspecting the part. The Izod test is particularly important in detecting overheated structure in medium carbon steels, and revealing laminations and improper heat treatment. The Izod, therefore, is an extremely valuable specification figure for judging whether a steel conforms to normal standards. Despite its usefulness the test is essentially obscure since it has not been correlated with other physical properties, such as damping and hysteresis to say nothing of simpler properties.

The discussion on impact testing sponsored by the Manchester Association of Engineers[30] (England) showed that considerable progress has been made in clarifying the meaning of the test but that there remains a large field for debate. Dr. Docherty's remark at the conference that when a crack had been started the energy required for its propagation varied greatly with the rate of testing was interesting but hardly supported by available notch impact results. The belief that the Izod test measures the resistance to the extension of a crack under fatigue conditions, as in a breathing boiler drum may be better founded, although here again information on crack propagation in fatigued material is a more appropriate subject for fatigue research. Laboratory confirmation of the fact that weld-metal with low Izod value cracks suddenly in boiler tests whereas weld-metal with high Izod cracks slowly has been obtained by Austin.[31] He showed that materials brittle in the Izod tests were brittle in internal pressure tests of tubes, although the static tensile strength and ductility of the brittle material were comparable with those of materials that were ductile in the internal pressure and Izod tests. Austin frankly confessed his inability to evolve an explanation of any sort for his results.

Important progress has been made in correlating results among the different types of impact tests. Sergeson and Poole[3] found that that it was very difficult to predict the temperature at which their steel (0.35 C, 0.55 Mn, 0.19 Si) would be unfit for service from results of tests on Charpy and Izod specimens with V and keyhole notches. Between +21° and −29° C. the Charpy V and Izod keyhole and V specimens showed a steady decrease from 50 to 5 ft.-lb. There was a transition zone (abrupt drop) with Charpy keyhole specimens.

In general, as Metal Progress[32] pointed out, four factors may be varied in notch impact testing: (1) temperature; (2) shape; (3) composition of specimen and (4) velocity. The relationship of temperature and specimen dimensions to fracture energy has been determined by Aldridge and Shepherd[33] for 2¼ Ni, 0.25 C steel normalized at 790° C., and drawn at 650° C. They studied the A. S. M. (Charpy keyhole), A. S. T. M. (45° V notch 2 mm. deep, 0.25 mm. radius), and Izod specimens (same notch but 0.67 mm. radius), Figs. 3 and 4. The ratio of results with A. S. T. M. to A. S. M. specimens was 1.60 at +21° C., 1.01 at −18° C. and 0.67 at −60° C., although the ratio of fracture areas was 1.60 and the ratio of mo-

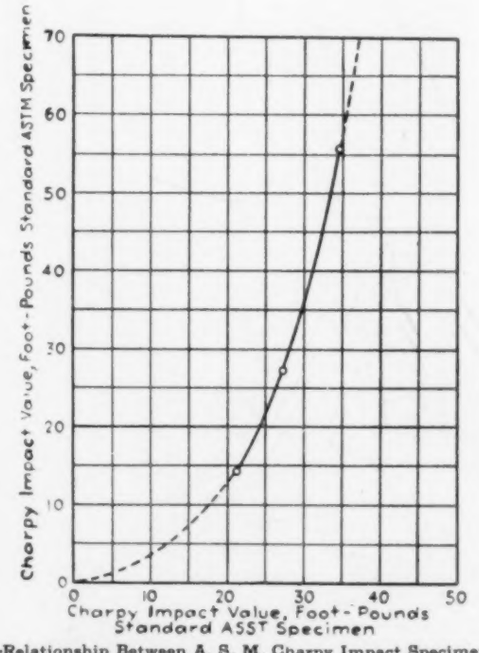

Fig. 3—Relationship Between A. S. M. Charpy Impact Specimen (Keyhole Notch) and A. S. T. M. Charpy Impact Specimen (Shallow V Notch). Aldridge and Shepherd[33]

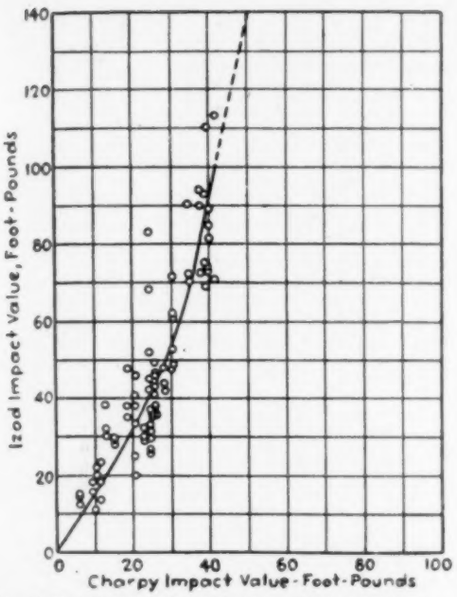

Fig. 4—Correlation Between A. S. M. Charpy Impact Specimen and Izod Impact Specimens. Aldridge and Shepherd[33]

ments of inertia was 4.10. As the width of the A. S. M. specimen was decreased from 10 to 2.5 mm., the multiplier required to reduce the results to the 10 × 10 mm. specimen decreased with temperature and was always lower than was expected from a comparison of the cross sections of fracture. When 1.3 mm. was added to the distance back of the A. S. M. notch the actual multiplier to be used for a specimen 5 mm. wide was decreased from 1.5–1.8 to 1.1–1.3 at +21 to −60° C. As the A. S. M. (Charpy) value increased from 10 to 40 ft.-lb. the multiplier increased from 1.5 to 1.8 with the 5 × 10 mm. specimen but decreased from 1.3 to 1.1 with the 5 × 11.3 mm. specimen. Whether or not the saw cut was the full width of the hole was immaterial in the keyhole specimens. Aldridge and Shepherd found that 40 ft.-lb. A. S. M. was equivalent to 90 ft.-lb. Izod, but 12 ft.-lb. A. S. M. was equivalent to only 20 ft.-lb. Izod.

The transition zone was distinctly present in Kinzel, Crafts and Egan's[34] Izod tests of mild steel between +20° and −78° C., and coincided with the temperature at which the shear cup and cone disappeared from the tensile fracture. Replacing the Izod notch with a keyhole eliminated the transition zone and reduced the sensitivity. The following factors had no effect on the low-temperature Izod results: (1) lapping the notch, (2) using a fly cutter to insure accuracy of form, (3) using a jig to locate the notch accurately, (4) stiffening the Izod vise, (5) small local temperature variations in the specimen.

With a notch of constant radius in sorbitic steels, the law of similarity for Charpy and Mesnager specimens was found by Yamada and Matuoka[35] to be $E = cbh^2$, where E is the absorbed energy, b and h are width of specimen and depth back of notch, respectively, and c is a constant. It would be surprising if Aldridge and Shepherd's[33] discovery of the effect of temperature on multipliers did not affect Yamada's constant considerably. At room temperature the Oxford and Izod specimens are closely proportional (50 to 90 ft.-lb./in.^{2}), according to Warlow-Davies and Southwell.[1]

The relationship between a number of different notch impact specimens for welds was found in an approximate manner at room temperature by Michel[36] to be:

Charpy 8 mkg./cm.^{2}	depth of notch	= 5 mm. (notch in side of weld)
Izod 8.5	"	"	"	"	= 2 mm.
Swiss 8.5	"	"	"	"	= 2.5 mm. (diameter of notch = 2 mm.)
DVMR 10	"	"	"	"	= 3 mm.
Mesnager 11.0	"	"	"	"	= 3 mm.

The same weld was used for all tests. According to Czternasty,[37] the notch impact value of weld-metal in the DVMR test is 0.7 times the value in the VGB test. For example:

6.7 mkg./cm.^{2} DVMR = 9.5 mkg./cm.^{2} VGB
10.7 mkg./cm.^{2} DVMR = 15.3 mkg./cm.^{2} VGB

The relationship was by no means so close in Christmann's[38] tests of annealed and unannealed 70° V oxyacetylene welds in mild steel. On weld-metal 5.7 DVMR = 6.0 VGB; 14.2 DVMR = 14.8 VGB. On unwelded base metal 9.3 mkg./cm.^{2} DVMR = 18.3 VGB; 11.0 DVMR = 25.7 VGB. Unnotched specimens (5 × 10 mm. cross section (Fig. 2)) of V butt welds in DuRietz's[25] tests did not have the same fracture energy as Charpy specimens with the same section modulus and cross section of fracture:

	Charpy		Unnotched	
Base metal	12	mkg./cm.^{2}	6	mkg./cm.^{2}
Bare electrode	1	"	2	"
Covered electrode	6	"	5	"

With the same oxyacetylene weld-metal, Brillié[39] found that Mesnager values of 16 specimens at 20° C. ranged from 14.7 to 20.4 mkg./cm.^{2} (average 16.0); the Charpy values of 15 specimens ranged from 4.5 to 12.5 mkg./cm.^{2} (average 10.0). The averages were exactly in the ratio of the cross-sectional areas of the two specimens; that is, 8 to 5, but the scatter was much higher with the Charpy specimens.

The search for the most sensitive impact specimen goes on, but without startling results. Mailänder,[40] and Jareš[41] have amassed a large amount of numerical information on the sensitivity of different forms of notch with special reference to German standard specimens. Round specimens with circumferential notches were favored by Wlodek,[42] whose investigation of the ratio between round and square specimens was confined to 20° C. Menghi[43] also preferred a specimen 10 × 14 × 55 mm. with a Mesnager notch on three sides of the specimen. According to Inglis,[7] the greater the theoretical stress concentration factor associated with a notch, the greater is the difference in impact value between room temperature and −30° C. With mild steel the ratio of impact values at the two temperatures was 0.44 for a factor of 3.82 and 0.25 for a factor of 10.64 (Izod notch).

The energy losses in impact testing are measured by Uebel[44] by replacing the fractured specimen in the vise and repeating the test. The method is valuable for cast iron and other brittle materials. Zeidler[45] found that different notch impact machines of the same type yield different impact results on identical specimens if the mass of the pendulums is different. Confirmation on welded specimens is supplied by Schoenmaker[46] who tested covered electrode all-weld-metal in the form of DVMR specimens on three machines. The smaller the capacity of the machines, the higher was the observed notch impact energy: 10.33, 10.88 and 11.92 mkg./cm.^{2} average on the three machines. A mathematical representation of temperature-impact relationships has been

evolved by Svetzova.[47] Judging from Weibull's[48] results on a number of steels at −80 to +100° C. the representation must be quite complicated.

The energy required to produce the first permanent set is regarded as significant by Welter and Kucharski[49] in their impact tests of notched cylindrical bars of alloy steel up to 500° C. The energy to fracture in the notched impact torsion test was also studied. A study of the relationships among angle of bend, temperature, fracture energy and diameter of specimen convinced Dejean and Simard[50] who used mild steel specimens with circumferential Charpy notches, that angle of bend is more important than fracture energy, at any rate from the standpoint of bars for reinforced concrete.

Standard Weld Tests

The French Welding Institute's[51] standard tests for filler metal (see Fig. 3 of earlier review on Impact Tests of Welded Joints) have been extended to include both oxyacetylene and covered electrodes. The Charpy or Mesnager specimens may be used, 10 mkg./cm.² Mesnager being equivalent to 6 mkg./cm.² Charpy. For some reason there are small differences in the impact requirements of the two types of filler metal having the same minimum static tensile strength.

A new German Tentative Standard Method for Notch Impact Testing of Butt Welds (DVM A122, April 1936) stresses the importance of placing the notch in the fusion zone, Fig. 5. No changes in the specimens themselves have been made, the VGB specimen being used, if the plate thickness permits. The notch impact test may be resorted to under three conditions: (1) if the weld is subjected to shock in service, (2) if the weld may be particularly brittle and (3) if the annealing is to be checked. Fiek[52] mentions that the notch impact test for welds is specified in Germany only in qualifying a method of welding, in welded vehicles, and in the German Boiler Code. The vehicles apparently represent shock in service, the welded boilers require annealing or normalizing in Germany, and a new method of welding may produce a weld that is more brittle than base metal.

The new German Boiler Code[53] specifies the DVMR specimen for plate less than 0.47 inch thick and the VGB specimen for thicker plate. The requirement for the large specimen varies from 8 to 12 mkg./cm.² Only the smaller specimen (DVMR) is used for testing the junction zone between weld and base metal.

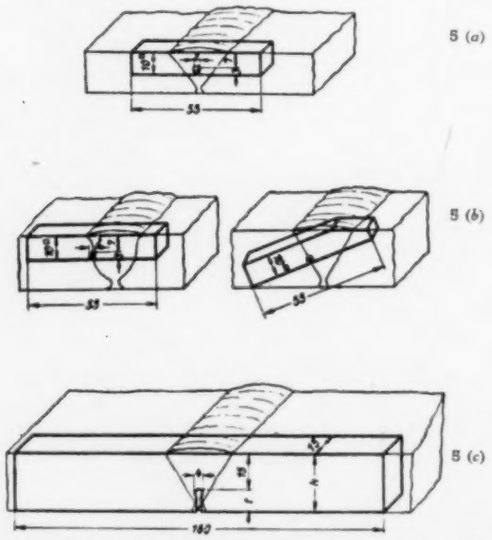

Fig. 5—German Tentative Standard for Notch Impact Testing Welded Joints (DVM A122)

(a) The usual location of the DVMR specimen with notch in root.
(b) For testing the junction zone between weld and base metal the surface perpendicular to the weld is ground and etched so that the notch may be accurately placed. If necessary and possible, the specimen may be cut at an angle to the surface.
(c) VGB specimen which may be used for thick material. t = depth of notch, h = plate thickness. Dimensions in millimeters

The Mesnager specimen with notch in side of weld is specified in the Czechoslovakian Standards[54] (CSN 1120). Six specimens are tested; the lowest value is discarded and the remaining five are averaged. Electrodes for light loads must give 3 mkg./cm.²; for heavy work 6 mkg./cm.² According to Portevin,[55] the Portuguese Navy requires minimum Charpy values for welds of high and low strength. The Italian Standard for Gas and Arc Welding in Naval Construction[57] requires that the average of four Mesnager specimens from a V butt weld attain 5 mkg./cm.²

In this country the A. S. M. E. Rules[56] for Containers for Gases and Liquids at Sub-Zero Temperatures down to −150° F. require a Charpy or Izod specimen, both with Charpy keyhole notch. Depending on plate thickness the width of the specimen may be 10, 5 or 2.5 mm. Three specimens with notch in weld-metal are averaged as well as three specimens with notch in heat-affected zone. The specimens are taken across the weld, are held at test temperature at least 30 minutes and are tested within 8 seconds after removal from the bath. The requirements are listed in Table 1.

Table 1—A. S. M. E. Impact Requirements (Ft.-Lb.) for Sub-Zero Welded Containers[56]

Cross Section of Specimen, Mm.	0 to −75° F.		−76 to −150° F.	
	Charpy	Izod	Charpy	Izod
10 × 10	15	18	10	10
10 × 5	10	10	4	3
10 × 2.5	5	4	2	1

A Charpy specimen (2 mm. drilled hole, A. S. M.) is included in the process approval test plates by the Navy[17] for welding carbon-molybdenum and related steels. The notch is placed in weld-metal, base metal and fusion line on both sides of the plate. The American Bureau of Shipping, Jackson[58] states, requires an Izod test specimen identical with Lloyds for qualifying weld-metal deposited by a new process.

The question is often posed whether the notch impact test is a suitable test to be included in specifications for welds. In view of the present widespread use of the test the answer appears to be in the affirmative if the material to which the specifications apply is subjected to severe conditions in service or is affected by those factors for the detection of which the notch impact test is peculiarly sensitive. Discussion of this question has been protracted and sometimes bitter. For example, Krynes[59] recommends discarding the notch impact test because there is no relation between it and fatigue tests. For that very reason, however, Schuster clings to the notch impact test for boiler welds in which a sudden brittle fatigue crack is far more dangerous than a gradual ductile fatigue crack. Polson and Sinfield,[60] as well as Stursberg[61] believe that the need for a "toughness" test for weld-metal cannot be denied, yet others adduce equally persuasive reasons for believing that the notch impact test depends too closely on testing conditions to be a reliable guide to any fundamental property or combination of properties of weld-metal.

Other Notch Impact Specimens

Reduced-width Izod, Charpy or DVMR specimens have been used by Okada,[62] Epstein, Nead and Halley[63]

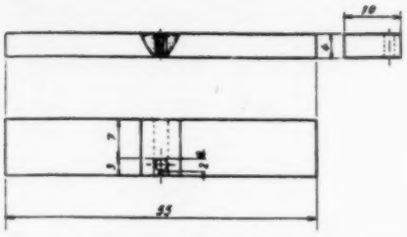

Fig. 6—Specimen Used by Stursberg[61] for Welds in Plate 0.24 Inch Thick. Dimensions in Millimeters

and Stursberg,[61] Fig. 6, respectively. Epstein, Nead and Halley's specimens were only $^1/_{16}$ or $^1/_8$ inch wide, yet there was no buckling of the specimen under the hammer. The absorbed energy was multiplied by the ratio of the width of the Charpy specimen (10 mm.) to the width of the specimen in order to obtain the "true" Charpy value. No justification for the multiplier was given despite the fact that tests were made over a range of temperature (+23 to −25° C.). Meunier and Rosenthal[64] as well as Dutilleul[65] and Pinczon[66] used Mesnager specimens with notch in the heat-affected zone created by a bead of weld-metal deposited on the surface of a plate. Pinczon's notch, Fig. 7, was perpendicular to the bead so as to include all parts of the heat-affected zone. The round Izod specimen, especially with curved notch, is not appropriate for the inspection of welded joints, according to Schuster,[2] because there is difficulty in machining the bottom of the notch parallel to the weld beads and in avoiding some unrefined top layer metal at the base of the notch. For judging the quality of filler rods or for research the round specimen is permissible.

Inglis[7] recommended a specimen $^7/_{16}$ inch diameter with $^7/_8$ inch Whitworth thread groove (55° angle, 0.071 inch deep, 0.015 inch radius) for Izod tests of weld-metal at elevated temperatures. A Tensometer[68] impact tester for welds delivers a blow of 3 ft.-lb. and requires a specimen $^1/_8$ inch diameter, $^3/_4$ inch long, with 45° notch, 0.003-inch radius. An unusual specimen, Fig. 8, was used by Nielsen.[69] An unmachined 60° V butt weld in plate 10 mm. thick was provided with a Charpy notch in the face of the weld so that the cross section of fracture was 7 × 10 mm. The root spacing in welding was 2 mm. and the testing span was 70 mm. The required fracture energy was 7 mkg./cm.^{2}

Unnotched, unmachined V butt welds have been tested by Strutskov[70] and Sikkema.[71] The former used the Charpy type of loading (simple beam) and struck the mild steel weld either at the root or on the face. The results with the root in tension usually were lower than with the face in tension, particularly if poor penetration was favored by the method of welding. Sikkema placed the edge of the weld level with the top of the Izod vise in an effort to detect brittleness in the topmost portion of the heat-affected zone in arc-welded low-alloy steels. Poor root fusion in arc-welded mild steel was detected

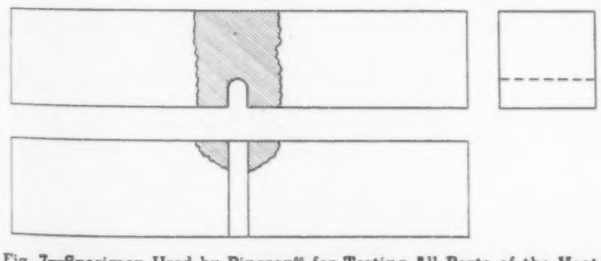

Fig. 7—Specimen Used by Pinczon[66] for Testing All Parts of the Heat-Affected Zone Created by a Head Deposited on the Surface of the Plate

by Quadflieg[72] using unnotched impact specimens. As the depth of poor fusion at the root increased from 0.3 to 1.0 mm. in a 10 × 10 mm. bar, the energy to fracture decreased from 24 to 10.5 mkg./cm.^{2} A specimen with perfect fusion required four blows to fracture. Quadflieg summed the energy absorbed in successive blows if more than one blow was required to fracture, although he admitted that the procedure was irregular.

Since it was impossible to machine a notch in welded 14% Mn steel, Csilléry and Péter[73] ground a shallow groove in the weld instead of a notch. For welded rail steel they used an unusual specimen with a cross section of 14 x 7 mm. back of the notch which was 4 mm. diameter, 7 mm. deep.

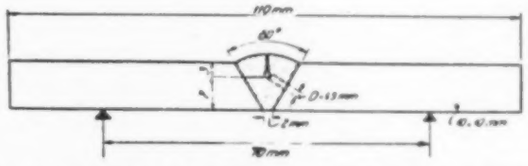

Fig. 8—Specimen Used by Nielsen[69] Is 0.39 Inch Square; Diameter of Notch = 0.051 Inch; Distance Between Supports = 2.75 Inches. Dimensions in Millimeters

Location of Notch

Since it is not an unreasonable requirement, as Shannon[74] points out, to demand uniform notch impact value across the weld, the location of the notch has been placed by different investigators and codes in different parts of the joint. Weld-metal itself has no well-defined directional properties so that the location of the notch is a factor only in testing the welded joint. There are still those who are skeptical about welding and regard the heat-affected zone as necessarily brittle in any steel. Like Lundgren[75] they prefer the notch in the heat-affected zone. Yet Blumberg and Hopkins[76] state that they no longer waste time notching the heat-affected zone of welds for pressure vessels because tests have shown the zone to have ample low-temperature Charpy value even in base metal that is brittle in the Charpy test at low temperatures. Exhaustive Izod tests by Nakamura-Hazime[77] on as-welded and annealed joints in mild steel and in cast steels with 0.10 to 0.30 C showed that the heat-affected zone was always superior to base metal. He arranged the notch so that the ratio of base metal to heat-affected zone or of heat-affected zone to weld-metal in the fracture varied from 2:1 to 1:2. Tests by Kinzel, Crafts and Egan[24] on welds in aluminum-treated S. A. E. X1020 and by Woirin[78] on arc-welded ship steels showed that the heat-affected zone was by no means a weak spot in the joint from the standpoint of notch impact value.

If anything, the heat-affected zone in the stress-relieved (650° C.) arc welds in normalized (820° C.) plates $1^3/_8$ inches thick containing 0.25 C, $2^1/_4$ Ni tested by Aldridge and Shepherd[33] had higher Charpy value than base metal. With notch perpendicular to the direction of rolling the base metal was 5 ft.-lb. higher than with notch parallel to the direction of rolling at both +21 and −46° C. In the heat-affected zone the Charpy value at both temperatures was 10 to 15 ft.-lb. higher with notch perpendicular than with notch parallel to the direction of rolling.

In order that the fracture should pass entirely through the heat-affected zone, Czyrski[79] did not scarf one of the plates forming the weld, Fig. 9. The other was scarfed to a larger angle than customary to secure good root penetration. The plates were 0.47 inch thick and were

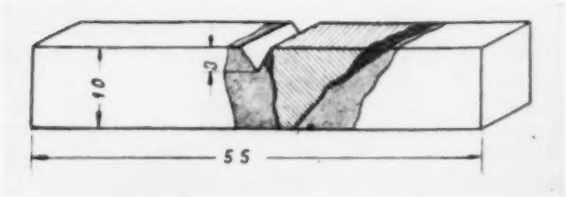

Fig. 9—Specimen Used by Czyrski[79] for Testing a Uniform Heat-Affected Zone. Dimensions in Millimeters

machined to DVMR (sharp notch 3 mm. deep, 45° V, 0.2 mm. radius) specimens with notch in the heat-affected zone of the unscarfed plate. Even in single layer welds the heat-affected zone had slightly higher notch impact value than base metal (0.15–0.70 C). A similar specimen was recommended by Newell,[80] but the notch was of Mesnager type and was located at the scarf to test the fusion line as well as a hardened zone, if present.

Whether the notch was perpendicular or parallel to the surface of the plate had no effect in Aldridge and Shepherd's[33] tests. However, Michel[36] preferred the notch in the side of the weld perpendicular to the surface so that all layers of the weld are tested at once. To what extent Michel believed the side notch removed any of the misleading aspects of the notch in other locations was not made clear. If the specimen were not machined, Michel's recommendation might have more force. Quadflieg's[72] tests on arc-welded mild steel (0.10 C, 0.42 Mn in weld-metal) showed that the scatter was less and the DVMR value up to 2 mkg./cm.^{2} higher if the notch was perpendicular to the surface of the weld than if the notch was in the root, Fig. 10. Quadflieg found that the fact that the root layers were 25% harder than the top runs, accounted for the higher values obtained if the root layers were not cut out by the notch. However, Quadflieg eventually favored the specimen with notch in root because the side notch does not correspond with the type of notch found in service.

It was immaterial in tests by Nehl,[81] Sikkema[71] and Csilléry and Péter[73] whether the notch was in the root or top layers of the weld. Czternasty[37] pointed out that there is a difference between specimens with notch in root and notch in top layers only if the scarf angle was incorrect and poor root penetration was obtained. Nevertheless, as Brillié[39] showed, if the top layer is not completely machined from the specimen, the notch in root will reveal lower notch impact value than the notch in the top layers. The difference between the two types of notches was only 2 mkg./cm.^{2} in 16 mkg./cm.^{2}

The root and the uppermost part of the fusion zone were the two locations favored by Block[82] for the VGB notch in boiler plate (tensile strength = 60,000 lb./in.^{2}, VGB value = 19.3 mkg./cm.^{2})4.3 inches thick, annealed at 9 10° C. after welding with covered electrodes. Specimens with the uppermost 0.59 inch of the fusion zone lying back of the notch averaged 19.7 mkg./cm.^{2} Specimens with the lowermost 0.59 inch of the root of the weld in the cross section of fracture averaged 16.7 mkg./cm.^{2}

Results of Notch Impact Tests

The notch impact testing of welds has been confined to steels, with one or two exceptions. The following factors have been studied:

1. Carbon content
2. Alloy content
3. Nitrogen
4. Welding procedure
5. Heat treatment

As far as possible, recent literature on each factor is discussed in separate sections.

Carbon Content.—As the carbon content of weld-metal increases, the notch impact value decreases, as shown by Zeyen.[83] The DVMR value (notch in root) of welds made with heavy covered electrodes decreased from 12.4 to 5.6 mkg./cm.^{2} as the carbon content of base metal increased from 0.11 to 0.68%. Although the weld-metal was not analyzed, doubtless it picked up carbon to an extent depending on base metal. The DVMR value of base metal decreased from 17.0 to 5.3 mkg./cm.^{2} in the same carbon range and oxyacetylene welds made with an alloy steel rod decreased from 9.8 to 2.0 mkg./cm.^{2} The notch impact value of welds made with an 18–8 electrode was 13 to 15 mkg./cm.^{2} independent of base metal. Aysslinger's[84] results with bare and heavy coated electrodes deposited in steels containing 0.3–0.5 C, 0.1–0.3 Si, 0.6–0.9 Mn showed about the same notch impact trend as Zeyen's tests.

An increase in carbon content of base metal also increases the ability of the steel to harden and become brittle in the heat-affected zone. To investigate the notch impact value of the heat-affected zone in single and multi-layer arc welds in 0.47-inch plates containing 0.08 to 0.70 C, 0.40–0.80 Mn, 0.21–0.36 Si, Czyrski[79] scarfed only one plate, Fig. 9, and used a German standard sharp notch (45° V, 0.2 mm. radius, 3 mm. deep) in the heat-affected zone of the unscarfed plate. Unwelded the impact value decreased from 16.3 to 1.0 mkg./cm.^{2} as the carbon content increased. Single and multi-layer welds improved the notch impact value to 14.5–1.1 and 20.2–2.5 mkg./cm.^{2}, respectively. Only in the 0.08 C steel was the single layer weld less tough than unwelded base metal.

Using a Mesnager specimen with notch parallel to and in the heat-affected zone created by depositing one or four beads of an electrode at 180 amps., 7 inches per minute on plates 0.59 inch thick containing 0.19 to 0.67 C, Meunier and Rosenthal[64] found that unaffected base metal decreased from 25 to 11 mkg./cm.^{2} as carbon content increased. Single layer deposits lowered the impact value to 16 at 0.19 C, and nil at 0.67 C, whereas four layers did not lower the notch impact value to any extent. In other words, Czyrski is at variance with Meunier and Rosenthal.

The weld and heat-affected zone in arc-welded cast steels containing 0.10 to 0.30 C, 0.4–0.5 Mn, 0.2–0.3 Si had higher Izod value in Nakamura-Hazime's[77] tests than the unwelded casting. Stress annealing at 650° C. and annealing at 900° C. were always beneficial, the Izod value being increased as much as 15 ft.-lb. in most instances.

Oxyacetylene welds were made by Holler and Franken-

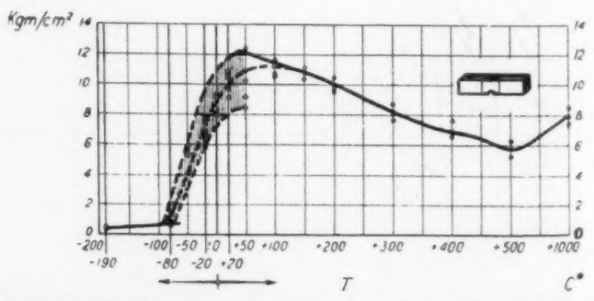

Fig. 10—DVMR Notch Impact Value as a Function of Temperature for Butt Welds in Plate 0.47 Inch Thick Containing 0.11 C, 0.54 Mn, 0.16 Si, 0.03 P, 0.032 S. The Weld-Metal Deposited by the Electrode Contained 0.096 C, 0.42 Mn, 0.2 Si, 0.017 N , 0.085 O_2, 0.027 P, 0.034 S. Notch Was Perpendicular to the Surface of the Weld. The Weld Was Not Heat Treated. If the Notch Was in the Root the DVMR Value Was Less Than 1 Mkg./Cm.^{2} at —80° C., and 8 at 0° C. Quadflieg[72]

busch[85] in steels with less than 0.10 C up to 0.33–0.37 C, 0.39–0.59 inch thick. Notch impact tests with the DVMR specimen indicated that the notch impact value as-welded depended on the quality of the rod, not on the carbon content of base metal. In all welds a reverse layer in the root was beneficial. Forging and peening also increased the notch impact value up to 7 mkg./cm.^{2} DVMR, particularly with low-grade rods. A 3% Ni rod did not produce remarkably tough welds in 0.22% C steel, nor were multilayers with this rod of any advantage from the standpoint of notch impact value. On the contrary, Rooke and Saacke[175] found the Charpy value of oxyacetylene welds made in one, two or three layers in a seamless pipe (0.24 C, 0.30–0.60 Mn, A. S. T. M. A-106-34T, Grade A, 8 inches diam.) was 15–16, 22, or 28–30 ft.-lb., respectively, if a rod containing $1^1/_2$ Ni was used. Unwelded pipe had 45–56 ft.-lb. Charpy.

Stursberg's[61] tests on oxyacetylene butt welds in steel containing 0.35–0.38 C, 0.66–0.83 Mn using four plain carbon (up to 0.58 C) and four low-alloy steel rods showed that the DVMR notch value depended primarily on the mixture ratio (ratio of oxygen to acetylene by volume). With a ratio of 1.0 to 1.1 welds made by all the rods had notch impact values (notch in root of weld) from 6 to 9 mkg./cm.^{2} The notch impact value was practically nil at a ratio of 0.8, presumably because the weld metals contained 1.8–2.5 C. With a ratio of 1.6 the notch impact value fell to 2 to 4 mkg./cm.^{2} The lowest notch impact value was obtained with a rod containing 0.58 C; the highest with a rod of unkilled, plain carbon steel (0.05 C).

A mysterious method of making butt welds in steel 0.20–0.71 inch thick containing 0.53–0.60 C, 0.58–0.68 Mn, 0.11–0.25 Si with an electrode 0.16 inch thick of unstated characteristics so that the weld had the same notch impact value as base metal (4.1 to 4.8 mkg./cm.^{2} DVMR) was announced by Brennecke.[85] In the absence of his statement to the contrary it may be presumed that he may have avoided testing the notch impact value of the heat affected zone, although his method of welding involved a reverse run.

Although rail steels have a high carbon content, the notch impact value at and near the oxyacetylene butt welds tested by Melhardt[87] was higher than the unwelded rails. Csilléry and Péter[73] regarded the notch impact test as an important indication of shock resistance of welded rails in service. Welds made with an electrode 0.16 inch diameter containing 0.5 C, 1.5 Mn, 0.6 V had several times higher impact value than unwelded rails. Two kinds of alloy steel electrodes were deposited in the same joint in some instances. If the combination of electrodes was well chosen, high notch impact value was obtained. The location of the notch (top, root, fusion zone) had no effect. Regardless of location of notch the impact value in most of the rail joints was higher than or at most slightly less than the unwelded rail. There was no relation between notch impact value and ductility in the static tensile test.

Steel Refining.—The grain size of a steel, particularly plain-carbon or low-alloy steel, has a pronounced effect on low-temperature notch impact value. It is well known that the fine grain size in the heat-affected zone of a multilayer weld results in ample low temperature notch impact value even in steels which are brittle at low temperatures. Although the grain size of base metal is controlled to some extent by the process steel refining, there is no connection between the process of steel refining used for the base metal and the notch impact value of weld-metal deposited therein. Hopkins and Blumberg[76] made A. S. M. Charpy tests on weld-metal and base metal in single V welded joints that had been normalized and drawn. The plates were 1 inch thick and were rolled from 100–123 ton heats containing 0.17–0.30 C, 0.38–0.66 Mn, trace to 0.26 Si, 0.015–0.15 Al, 0.007–0.045 Al_2O_3. They were silicon killed with or without small or large additions of aluminum, or semi-killed, or rimmed. The notch impact value of the weld depended solely on the electrode, which, if suitably chosen (no details), could be relied on for 15 ft.-lb. or so at −100° C.

Welds made with mineral or combustible covered electrodes in aluminum treated S. A. E. X1020, $^1/_2$ inch thick had Izod values of 45 to 76 ft.-lb. at +20° C., 15 to 50 ft.-lb. at −78° C. and 4 to 31 ft.-lb. at −120° C., according to Kinzel, Crafts and Egan.[34] With notch in scarf the Izod was 80 ft.-lb. at −78° C. and 15 to 90 ft.-lb. at −120° C. Obviously the treatment with aluminum was beneficial to base metal. However, in plate $1^1/_2$ inches thick the scarf had only 3 to 10 ft.-lb. at −120° C. Stress relieving at 650° C. slightly raised the Izod of scarf at +20 and −50° C. but had no effect at −120° C. or on the weld. Normalizing at 900° C. was not advantageous.

Special basic open-hearth steel (0.39 inch thick, 0.10 C, 0.2 Mn, 0.25 Cu) made without manganese was welded by Wilhelm[88] and tested as DVMR specimens at 0 to +50° C. Oxyacetylene, D.C. arc, atomic hydrogen and force welds had low notch impact value at 0° C., 3 to 11 mkg./cm.^{2} at +20° C. and 6 to 13 mkg./cm.^{2} at +50° C. The results compared favorably with results obtained from steel made with manganese.

Low-Alloy Steels.—The notch impact test has been popular with investigators of welded low-alloy steel. Most of the investigators were careful to notch the heat-affected zone as well as the weld-metal. As a matter of fact, Armstrong's[89] Izod study of welded Ni-Cr steel (0.29 C, 0.26 Mn, 3.25 Ni, 1.35 Cr, water quenched from above Ac_3, tempered at 580° C., $^5/_8$ inch thick, 6 inches square) was confined entirely to the heat-affected zone created by a bead of weld-metal deposited on the surface by heavy organic coated mild steel electrodes $^1/_8$ and $^5/_{32}$ inch diameter. In addition to being located in unaffected base metal the notch was located on the opposite side from but directly below the ground-off bead (low impact results), and across the ground-off bead (also low results), as well as in the side of the plate at 90° C. to the weld in the unaffected area above the weld (results 10 ft.-lb. higher than the two preceding specimens). Base metal was 60 Izod, heat-affected zone was 30 ft.-lb Izod without preheat. However, preheating above 100° C. also lowered the impact value of base metal to as low as 21 ft.-lb. with 680° C. preheat. Even the latter value, which was the lowest recorded by Armstrong, does not characterize the steel as brittle.

Notching of the heat-affected zone beneath a bead of arc deposited weld-metal was also the method employed by Meunier and Rosenthal[64] in studying steels containing 0.14–0.24 C, 0.4–1.3 Mn, with fractional percentages of Cu, Cr or Mo. The Mesnager value after four beads had been deposited was higher than unwelded base metal. A single bead lowered the impact value 2 or 3 mkg./cm.^{2} which was not important. In describing their experimental conditions Meunier and Rosenthal took pains to state that the plate thickness was 0.59 inch and the welding velocity was 7 inches per minute. Welding velocity or any other index to the maximum rate of cooling in the vicinity of the transformation ranges of the steel under consideration is an important factor in assessing the results.

Meunier and Rosenthal's steel containing 0.24 C, 0.47 Mn, 0.27 Si, 0.47 Cr, 0.55 Cu, 0.14 Mo yielded 10 mkg./cm.^{2} in the heat-affected zone under a single bead compared with 13 mkg./cm.^{2} for unwelded base metal. The

conclusion to be drawn is that the heat-affected zone was probably not very tough because a considerable proportion of the material back of the notch was not heated to a high temperature during the deposition of a single bead.

Stress relieved two-pass and three-pass D.C. arc weld-metal tested by Aldridge and Shepherd[32] (1.6 Ni, 0.10 C) had 36 ft.-lb. Charpy at 20° C. and 22 ft.-lb. at −60° C., which was the same as fine grained, rolled 0.09 C steel perpendicular to the direction of rolling or slightly below rolled 0.25 C, 2.4 Ni steel parallel to the direction of rolling. The heat-affected zone in base metal (2.33 Ni, 0.45 Mn, 0.14 C) had the same Charpy value (36 ft.-lb.) at −60° C. as unwelded base metal, whether or not stress relieved. Stress relief raised the Charpy value of weld-metal only 4 ft.-lb. at −60° C. Szumowski[90] reported 25 to 43 ft.-lb. Charpy for oxyacetylene welds containing 0.20 C, 0.82 Mn, 0.54 Si, 2.09 Ni. Arc welds with 0.24 C, 0.65 Mn, 0.16 Si had 36 to 65 ft.-lb. Charpy. Larson[91] found that the Charpy value of welded 2½ Ni steel was acceptable at −60° C. even after 72 hr. at that temperature.

Butt welds in copper steel (1½ inches thick, 0.24 C, 0.64 Cu) made with an electrode 0.16 inch diameter containing 0.7 Cu, 0.2 Mo and some nickel had a uniform VGB impact of 12.5 to 17.5 mkg./cm.², according to Nehl,[81] if the joint was reheated to 860–880° C., cooled slowly to 500° C., raised to 640–660° C. and air cooled. If the joint was simply air cooled from 860–880° C., omitting the "stabilizing" heat treatment, the weld-metal had only 3.3–8.7 mkg./cm.² VGB. Using specimens with Charpy notch but only 1/16 or 1/8 inch wide, Epstein, Nead and Halley[63] found that the heat-affected zone of oxyacetylene and covered electrode welds in four low-alloy steels (0.08 C with or without 1 Cu, ½ Ni, 0.3 Cr, 0.1 P) had nearly the same notch impact value as base metal at +23° and −25° C. There was only a slight difference among the impact values at the two temperatures. The welds were generally 10 to 50% lower at −25° C, than at +23° C. The heat-affected zone in arc-welded steel containing 0.17 C, 2 Ni, 1 Cu had nearly twice as high notch impact value (80 ft.-lb. V notch Charpy, no details) as base metal (40 ft.-lb.) in Gibson's[92] tests unless the joint was air cooled from 900° C. followed by reheating 1 hr. at 510° C. Stress annealing or normalizing raised the notch impact value of the heat-affected zone. Oxyacetylene butt welds in a steel 0.39 inch thick containing 0.18 C, 0.60 Si, 1.3 Mn, 0.40 Cu made with a low-carbon rod having some Ni, Mn and Cu, according to Holler and Frankenbusch,[85] had 7.4–8.3 mkg./cm.² DVMR compared with 6.9–7.6 mkg./cm.² for unwelded base metal. A reverse bead raised the DVMR value to 8.7–9.6 mkg./cm.² Peening had less effect than the reverse run. Zeyen[93] found that the notch impact value of arc-welded low-alloy steel at 200° C., which was the preheating temperature, was no lower than at room temperature.

Using an unnotched, unmachined specimen (10 x 10 mm. cross section), Sikkema[71] found that the hammer was stopped but that the joint (80° V butt) with higher notch impact value in the weld underwent a greater angle of bend. The steels contained 0.18 C, 1.1–1.5 Mn, 0.3 Cr, up to 0.6 Cu. The less ductile weld-metal (no details) had 7–8 mkg./cm.² DVMR; the more ductile had 11–13 mkg./cm.² The vise gripped the weld at the edge of the reinforcement. Czternasty[37] found 5–9 mkg./cm.² in an oxyacetylene weld containing 0.11 C, 0.7 Mn, 0.7 Ni. Annealing and hot peening raised the DVMR value to 9–12 mkg./cm.² The DVMR value of an oxyacetylene weld containining 0.10 C, 0.7 Cr, 0.4 Mo, 0.4 Si, 0.5 Mn was 10–22 mkg./cm.² Deposits from an electrode covered with a compound containing 25% TiO_2, 25% SiO_2, 50% MnO were obtained by Tichodeev[24] with 0.1 C, 0.6 Mn, 0.1 Si, and up to 1.4 Cu, 2.4 Ni, 0.18 Mo had 35 to 58 ft.-lb. Mesnager. Okada[62] found that arc welds containing over 0.4 Si with 0.8 to 2.2 Mn generally had lower Izod values than deposits containing less than 0.4 Si with 0.6 to 1.5 Mn; but there were exceptions.

In the Low-Alloy Steel Committee's[95] presentation of the properties of gas and arc-welded steels a great many determinations of Charpy value are given. The values range from 20 to 65 ft.-lb., but no indication of the significance of the Charpy test for welded alloy steels was suggested. In Lent's[96] tests, oxyacetylene welded Sicromal (6–22 Cr, 0.5–4 Al, 0.79 inch thick) for superheater tubes had notch impact values (probably VGB) of 5 to 9 mkg./cm.² in weld-metal and 13 to 18 mkg./cm.² in heat-affected zone.

Austenitic Steels.—Austenitic filler metal is sometimes used for welding mild steel. Welds made with a mineral covered electrode containing 25 Cr, 12 Ni in aluminum treated S. A. E. X1020 had 70 ft.-lb. Izod at +20° C., 60 ft.-lb. at −120° C. The heat-affected zone of the mild steel had the usual notch impact value, according to Kinzel, Crafts and Egan.[34] Heat treatment (2 hr. 600° C., furnace cool, or ½ hr. 850° C. air cool) has no effect on different grades of austenitic weld-metal, the DVMR values of which, states Zeyen,[97] are 11 to 20 mkg./cm.² for arc welds and 10 to 11 mkg./cm.² for oxyacetylene. The fusion zone[98] of double V austenitic welds in 1 inch Izett IV steel was 13 to 19 mkg./cm.² DVMR as-welded or heat treated. Kalisch[99] found that the DVMR value of austenitic welds in Cu-Mo boiler steel (0.17 C) was 15–17 mkg./cm.² as-welded, 10–13 mkg./cm.² after strain aging, presumably at 250° C. As might have been expected the DVMR value of welds[83] made with 18–8 electrodes in plain carbon steels with 0.11 to 0.68 C was 13–15 mkg./cm.² independent of base metal.

The DVMR impact value of arc welds in stabilized 18–8 (0.10 C), according to Hougardy,[100] is 14.0 mkg./cm.² with notch in weld and 15.9 mkg./cm.² with notch in heat-affected zone. Base metal had 19.7 mkg./cm.²

Arc welds in 14% Mn steel rails had 100% greater notch impact value than unwelded base metal in Csilléry and Péter's[73] tests. The notch was a shallow groove ground in the weld or fusion zone.

The Nitrogen Question.—If it were only nitrogen that contaminated some weld metals, its effect on notch impact value could be predicted with little difficulty. But oxygen and perhaps other factors combine to make the nitrogen question in welds a trifle perplexing. Since Kleinefenn's exhaustive investigation, which was discussed in the earlier review on Impact Tests of Welded Joints, very little new information has been uncovered. Zeyen's[101] DVMR impact tests at −70 to +400° C. showed that the transition zone for the heavy covered electrode deposit which he employed was −50° C. Dipped and alloy cored electrodes yielded deposits having higher transition zone (about 0° C.). Normalizing (½ hr. 900° C.) or stress relieving (2 hr. 600° C.) had no effect on the transition zone of the covered electrode deposit and raised the notch impact value, but raised the transition zone of the alloy cored electrode deposit to +20° C. Zeyen believed the nitrogen contents, 0.11 for cored, 0.026 for covered, explained the results. Leitner[102] agreed with Zeyen that the alloy cored electrode deposit has only one-half the DVMR value of covered electrode deposits at room temperature. Contrary to Zeyen, however, Leitner maintained that at −20° C. both cored and covered electrode deposits had 4 mkg./cm.² DVMR. It is believed that Zeyen's results remove much of the unknown about the cored electrode.

Nitrogen was also believed by Zeyen to account for the

fact that 10% compression followed by ½ hr. at 250° C. lowered the DVMR value of covered and cored deposits at 20° C. to 4–6 mkg./cm.² and 1 to 1.5 mkg./cm.², respectively. Pure iron-nitrogen alloys would not be expected to behave in this way, but other impurities supply the explanation. Below 10 mkg./cm.² DVMR Zeyen found a fairly close proportionality between bend angle and DVMR value, but there was no connection with static tensile ductility.

An extensive investigation led Quadflieg[72] to conclude that nitrogen (oxygen as well) is a less important factor for notch impact value than grain size and pearlite distribution, unless the nitrogen content is rather high (< 0.03%) in which event the needle structure has the best notch impact value. Rapid cooling or very slow cooling, the latter of which results in "globular" nitride, spoils the notch impact value. Schuster's[115] results confirm the effect of slow cooling which lowers the Izod value of lower run metal from 20–45 ft.-lb. with rapid cooling to 8–18 ft.-lb. with slow cooling. Schuster, however, believed that the appearance of nitride needles (no nitrogen analyses were reported) in the slowly cooled specimens accounted for the lower Izod value and the higher temperature of the transition zone. The coarser the needles the lower was the notch impact value. Even allowing for differences in cooling rate used by Quadflieg and Schuster, the two investigators are not in agreement. Other factors than nitrogen must account for the lack of agreement.

Low-alloy steel as well as plain carbon steel is subject to lowering of notch impact value by nitrogen picked up in welding, according to Thierens.[103] That different alloying elements exert different effects was shown by Svegintsev,[104] who tested deposits from different electrodes covered with the same coating: 3 parts by weight pyrolusite, 1 part Fe_2O_3, ½ part $CaCO_3$, 0.05 part Al_2O_3, in water and waterglass (23–25° Bé.). Stress relief at 650° C. did not lower the Mesnager value, but annealing at 920° C. lowered the notch impact value from 11 to 2 mkg./cm.² Precipitation of nitride needles and segregation of cementite were believed to account for the results. Manganese (1.45%) in the electrode was lost during deposition, and had no effect on nitride brittleness. On the other hand, 1 to 4.6% nickel in the electrode was said to prevent nitride brittleness.

Exactly opposite results were secured by Diatlov.[105] Water quenching from 650° C. followed by holding 10 days at room temperature created nitride brittleness. If the specimens were tested immediately after being quenched, the transition zone (Mesnager specimen with notch in root of V butt weld) was 0 to −20° C. for ordinary coated electrode welds; holding 10 days at room temperature raised the transition zone to +150° C. Entirely different results were obtained if the rod contained 0.11–0.15 C, 1.2–1.5 Mn (weld-metal contained 0.1 C, 0.5 Mn) or was coated with 27% ilmenite, 27% feldspar, 15% ferromanganese (70% Mn), 13.5% pyrolusite, 10% sawdust, 12.5% waterglass. After the aging treatment the welds had 12 mkg./cm.² at 100° C. and 5 mkg./cm.² at −40° C., which is satisfactory. Molybdenum was less effective than manganese, and nickel had scarcely any effect. Diatlov believed, mistakenly, that Mn and Si are effective in decreasing nitride brittleness because they form nitrides.

The nitrogen question is always closely related to electrode coatings. Jennings[106] and Mathias[107] stated that bare, dusted, sulcoat or washed electrodes deposit metal having 5 to 15 ft.-lb. Izod; covered, shielded arc metal has 30 to 50 ft.-lb. E. W. P. Smith[108] is more optimistic and assigns 50 to 80 ft.-lb. Izod to the shielded arc deposits. With electrodes containing less than 0.10 C, Whittemore[109] found that deposits from a bare rod had 5–7 ft.-lb. Charpy, medium coated 18–25 ft.-lb., and heavy coated 25–35 ft.-lb. Charpy (68 ft.-lb. Izod). Some conception of the effects of coatings is given by the experiments of Granjon and Brillié,[110] which were about the same for all-weld-metal and welded joints. With the same core rod and coating thickness the Mesnager value was less than 1 mkg./cm.² for an inefficient coating, 8 to 14 mkg./cm.² for a fusible coating, and 15 to 20 for a coating of combustible type. If the fusible coating was too thin, the Mesnager value dropped to 4 mkg./cm.² For comparison an oxyacetylene deposit made with the same rod had 6 to 18 mkg./cm.²

Russian experimenters are particularly communicative about the composition of their coatings. Kusmak and Abramova[111] tested 15 coatings containing a variety of slag forming constituents, and concluded that no single constituent can be relied upon to produce welds low in oxygen and nitrogen unless the remainder of the coating is properly compounded. TiO_2 was superior to Mn in reducing the oxygen and nitrogen content of weld-metal (0.10 C) and in maintaining high Mesnager value despite unfavorable heat treatment, such as slow cooling from 900° C., or holding 12 days at room temperature after water quenching from 650° C. Although TiO_2 was not essential, one coating, which contained 22.5% TiO_2, produced weld-metal with 0.12 C, 0.46 Si, 0.33 Mn, 0.035 N_2, 0.118 O_2. The Mesnager value at 20° C. was 10 to 14 mkg./cm.² regardless of heat treatment. All the deposits in this investigation had about 20% elongation in 5 diameters in the static tensile test, yet the Mesnager values ranged from 1 to 14 mkg./cm.²

In an investigation of the low temperature Mesnager value of welds made with electrodes 0.16 inch diameter with coatings 0.02 to 0.04 inch thick, Diatlov and Kasimirov[112] found that thick coatings, for example, 30% ilmenite, 15% pyrolusite, remainder ferromanganese and waterglass, gave the best low temperature Mesnager value. The electrodes contained 0.1 C, 0.4 Mn, 0.02 Si; the plates 0.23 C, 0.6 Mn, 0.3 Si. Additions of copper or nickel to weld-metal raised the Mesnager value at −40° C. higher than unwelded base metal. Zschokke[113] reported that all-weld-metal from an electrode containing 0.04 C, 0.43 Mn, 0.01 Si coated with 12.4% SiO_2, 30.7 $NaSiO_3$, 23.6 Fe, 17.9 Mn, 5.6 CaO, 4.9 CO_2, 2.5 Al, remainder moisture, had 6.8–7.1 mkg./cm.² DVMR when d.c. was used, 7.2 to 8.1 mkg./cm.² with a.c. The essential constituents of the coating were chalk, silicates, Mn and Al.

Heat Treatment.—A major application of the notch impact test is as a guide to the correct heat treatment of steels, particularly temper brittle steel and boiler plate. In the broadest sense, welding is a form of heat treatment for the heat-affected zone of base metal. Furthermore, welded joints are often heat treated. According to Quadflieg,[72] ferrite grain size and distribution of pearlite are the determining factors for the notch impact value of covered electrode welds in mild steel (0.11 C, 0.55 Mn) and low-alloy steel (0.18 C, 1.0 Mn, 0.5 Si, 0.5 Cu, 0.4 Cr). Rapid cooling (water quench from 850° C., draw 600° C.) from above Ac_3 produces a fine ferrite grain size and high DVMR value at low temperatures (−50 to 0° C., but tests were made as low as −190° C.). Slow cooling, on the other hand, results in coarse ferrite grain size, low notch impact value at low temperatures but unusually high values at elevated temperatures (+300 to +600° C.). Quadflieg concluded, therefore, that normalizing was best for welded boilers, but more rapid cooling together with drawing was required for vessels operating at low temperatures. In Quadflieg's opinion, rapid cooling from Ac_3 suppresses the inoculating

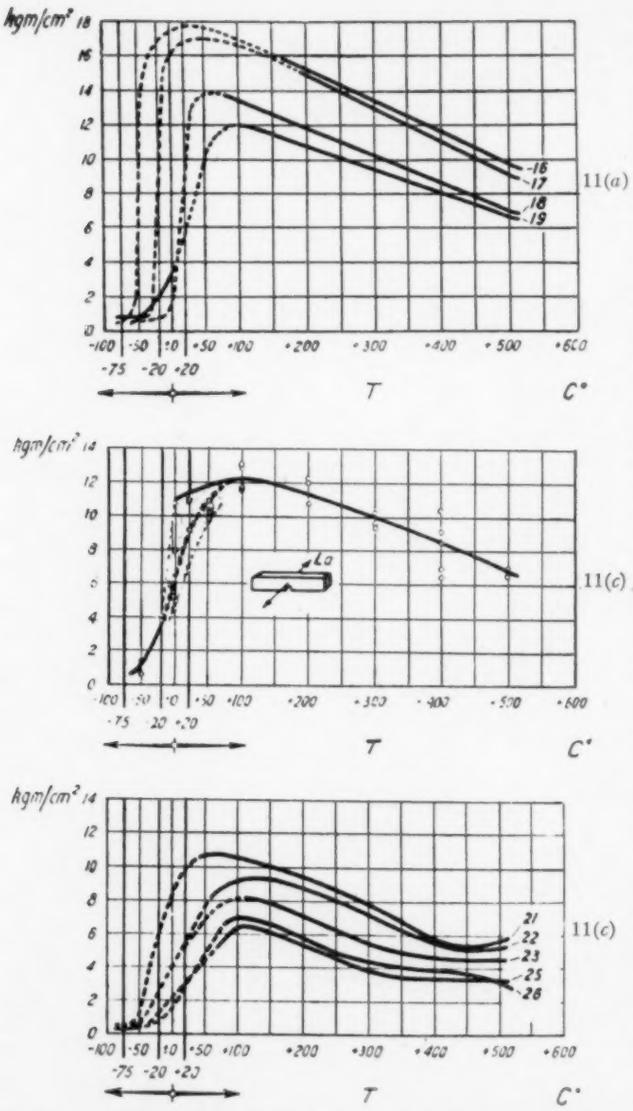

Fig. 11—(*a*) DVMR Notch Impact Value as a Function of Temperature for Boiler Plate 0.79 Inch Thick Containing 0.11 C, 0.36 Mn, 0.14 Si, 0.034 P, 0.024 S

(*b*) Same as (*a*) but Specimen Elongated 20% in Tension, Allowed to Remain 6 Weeks at Room Temperature; Axis of Notch Parallel to Direction of Rolling and of Tension

***La* = Direction of Rolling and Tension**

(*c*) Same as (*a*) but Welded with an Electrode Depositing Metal Containing 0.10 C, 0.44 Mn, 0.12 Si, 0.015 N_2, 0.07 O_2, 0.024 P, 0.033 S

Curve 16—as-rolled
Curve 17—10% elongation in tension, artificial aging, axis of notch perpendicular to direction of rolling and of tension
Curve 18—10% compression, artificial aging, axis of notch parallel to direction of rolling
Curve 19—10% elongation in tension, artificial aging, axis of notch parallel to direction of rolling
Curve 21—as-welded
Curve 22—5% elongation in tension, artificial aging; axis of notch parallel to direction of tension
Curve 23—10% elongation in tension, artificial aging; axis of notch parallel to direction of tension
Curve 25—15% elongation in tension, artificial aging; axis of notch parallel to direction of tension
Curve 26—10% compression, artificial aging, axis of notch parallel to direction of tension. Quadflieg[72]

effect exerted by inclusions and in this way prevents the formation of coarse ferrite. As Block[114] points out, multi-layer welds have a coarse grained top layer, which has low notch impact value in which cracks easily form. Confirmation of the poor Izod value of top-run metal is provided by Schuster,[115] who found 3 ft.-lb. in the top run of a weld having 25 ft.-lb. in the lower layers. Lefèvre[67] found that the top run of weld-metal deposited by covered electrodes 0.28–0.32 inch diameter had 5.4–6.5 mkg./cm.² Charpy, whereas the lower runs had 8–9 mkg./cm.²

A complete investigation of strain aging tests also was made by Quadflieg. Some foreign welding codes call for notch impact tests on welds strained 10% in compression or tension followed by holding (aging) 1/2 hr. at 250° C. Exhaustive tests, Fig. 11, convinced Quadflieg that aging at room temperature (nitrogen content of weld was 0.01–0.02%) resulted in lower notch impact values than holding at 250° C. If the nitrogen content was less than 0.01%, Quadflieg admitted that holding at 250° C. might exert greater effect than holding at room temperature. The greater the strain, 5 to 15%, the greater was the decrease in notch impact value and the higher the transition zone. Compression seemed to have a greater effect than tension. The notch impact value was lower when the notch was parallel to the direction of strain than perpendicular, which was explained on the basis of the combined effect of hot and cold work in aligning the crystals. Obviously, many variables exert important effects on the strain-aging test. Quadflieg also showed that cold work due to welding stresses in rigid welding may reduce the notch impact value up to 25%. Before Quadflieg's tests were published, Electric Welding[116] stated that it was an open question whether shrinkage stresses affected notch impact value. Quadflieg, like earlier investigators of the same problem, who, however, found no effect, cut his specimens from the finished weld and in so doing relieved most of the shrinkage stresses. Consequently, his conclusion concerning cold work cannot be extended to include shrinkage stresses. Nevertheless, Quadflieg suggested that the notch impact test could be used to estimate the degree of rigidity of the welding, but it is easy to disagree with his suggestion.

In Lea and Parker's[117] Izod tests of 70° V butt welds made with blue asbestos covered electrodes in 1/2 inch mild steel plates care was taken that the bottom of the notch (round Izod specimens) was below the top run. The transition zone of as-welded specimens was at 0 to −20° C. The Izod values were 35 to 80 ft.-lb. at +100° C., 15 to 25 ft.-lb. at 500° C. Both annealing and normalizing improved the Izod value 20% at +100° C., but did not change the transition temperature or the Izod value at 500° C. The Izod value of normalized welds was unchanged by heating nearly 4 months at 250 or 450° C. with or without a tensile stress of 12,000 lb./in.² The specimens were tested within 24 hr. of removal from the furnace. The Izod value at +20° C. dropped from 25 to 10 ft.-lb. after 45 days at room temperature following a water quench from 650° C. The weld contained 0.06 C, 0.65 Mn, 0.066 N_2.

Quenching from 650 or 750° C. with or without subsequent treatment at 100° C. for 2 hr. had little effect on the DVMR impact value of the covered electrode welds in boiler plate tested by Aysslinger.[118] At −60° C. the plate (0.20 C, 0.44–0.77 Mn, up to 0.34 Si) was brittle in the transverse direction (0.3–1.0 mkg./cm.²), whereas the weld had 4 mkg./cm.² the fracture showing considerable ductility. Strain aging tests lowered the DVMR value of plate and weld at 20° C. from about 10 mkg./cm.² to 3–5 mkg./cm.²

The heat effect due to welding had a much more beneficial effect on the mild steel used by Nakamura-Hazime[77] than annealing at 650, 800 or 900° C. Unwelded base metal had an Izod value of 25 ft.-lb., weld-metal was 72 ft.-lb. and the heat-affected zone was 45 to 68 ft.-lb. The annealing treatments raised base metal to 26–38 ft.-lb. but had no important effect on heat-affected zone or weld-metal.

The effect of annealing on the notch impact value of high quality arc welds in boiler plate, according to Wan-

delt,[53] depends on the smaller details of heat treatment. For example, a weld heated 9 hr. at 900–940° C. and cooled in the closed furnace had the comparatively low DVMR value of 8–12 mkg./cm.², and the pearlite was not uniformly distributed. Reheating the same weld 1¾ hr. at 900–910° C., cooling in the open furnace to 650° C., followed by cooling in the closed furnace increased the DVMR value to 20–25 mkg./cm.²

Butt welds in Izett IV plate 1⅜ inches thick made with aging-resistant steel electrodes, of which Aureden[119] gave no details, had 16 mkg./cm.² VGB with notch in fusion zone regardless of stress relief at 600° C. or normalizing at 920° C. Normalizing raised the VGB value of weld-metal from 13 to 17 mkg./cm.²; stress relief had little effect. A French turbine manufacturer[120] found that stress relief at 650° C. had no effect on the Mesnager value of arc welds in plain carbon steel 1.1 to 1.6 inches thick. Fillet-welded mild steel (0.17 C), according to Mladiata,[121] who used a notch impact specimen cut from the heat-affected zone having a cross section 6 x 10 mm. back of the notch, is reduced 10% in notch impact value by annealing 2 hr. at 920° C.

The possibility of short time heat treatment for butt-welded joints was investigated by Okada.[62] He heated V butt welds made with bare and coated electrodes (0.135 and 0.041 N_2, respectively) for a short period in a resistance welder to temperatures from 100 to 1300° C. or for ½ hr. in a furnace followed by air cooling. The resistance heated specimens were heated and cooled very rapidly, from 1300 to 300° C. in 2 minutes, for example, and the welds were reduced in section to concentrate the heat. Single beads of the coated electrode had 3 to 5 ft.-lb., seven-layer deposits 13 to 17 ft.-lb. practically independent of temperature of furnace heat treatment. The notch impact specimen for the single bead deposit had a cross section of fracture of 4 x 10 mm. with a 45° notch 1 mm. deep. Izod specimens were used for the seven-layer deposits. The bare electrode deposits were brittle under all conditions. Erratic and, on the whole, unsatisfactory results were obtained with short time heating, which is not surprising in view of the peculiar method of heating.

Torch annealing of arc-welded mild and low-alloy steel (covered electrodes) was shown by Thierens[103] to have the same negligible effect on DVMR impact value as furnace normalizing. Contrary to other investigators, Thierens found that stress relief improved the DVMR value in the low-alloy steel by 25%. Keel[122] recommended torch annealing for adding 30% to the notch impact value of oxyacetylene welds.

In the section on Low-Alloy Steels it was stated that annealing and hot peening appear to be beneficial for the notch impact value of low-alloy steels. Holler and Frankenbusch[85] showed that oxyacetylene welds in plain carbon steels made with low-grade mild steel rods may be improved 300 or 400% in notch impact value by a reverse layer or annealing at 900° C. or by peening at a red heat. Resort to these measures was hardly worth while from the standpoint of notch impact value, if high-test rods were used. Blumberg[123] found that the effect of peening on notch impact value depended on the peening procedure and was sometimes disadvantageous.

In Christmann's[38] tests of 70° V oxyacetylene welds in mild steel (tensile strength = 68,000 lb./in.²) for compressed gas tanks 0.59 inch wall thickness, normalizing 20 minutes at 900° C. raised the DVMR and VGB values from 6 to 14 mkg./cm.², despite the fact that the root of the weld had been ground out to a depth of 0.2 inch and reverse welded. The extensive investigations by Leitner and Schmidt[124] and by Matting and Otte[125] on several plain carbon steels with plain carbon and nickel-manganese rods are in agreement that annealing and normalizing (about 940° C.) raise the DVMR value from 50 to 300%, depending on the quality of the rod. However, Matting and Otte appeared to favor forehand welding from the notch impact standpoint, whereas the other investigators detected no difference in notch impact value between the two procedures. Stress relief at 600° C. had no effect on DVMR value.

In Mercier's[126] tests of oxyacetylene welded Mesnager specimens in steel ½ inch thick containing 0.13 C, 0.46 Mn, 23 mkg./cm.² Mesnager, left-hand and right-hand welds had practically the same Mesnager value. Welds made in two passes were slightly tougher than welds made in a single pass. Two-torch vertical welds had more consistent Mesnager values than welds made by the older processes. Stress relief at 650° C. for 10 min. raised the Mesnager value a little, but 6 to 12 min. at 850° C. or 3 to 10 min. at 950° C. had a most salutary effect, the shorter times being the more effective. The short annealing raised the Mesnager values from a range of 2–10 mkg./cm.² as welded to 11–18 mkg./cm.² Torch annealing was definitely recommended by Mercier for improving the notch impact value. Unfortunately Mercier did not state the composition of his rod and was vague in attributing the decrease in notch impact value with increasing annealing times to grain growth. The carburizing flux technique was not studied.

In oxyacetylene welding mild steel (tensile strength = 59,000 lb./in.²) 1⅜ inches thick with a mild steel rod, Rist[127] used double V scarves and peened each 2-inch length of weld-metal while it was red hot, using a compressed air hammer. The VGB notch impact value depended on the location of the cross section of fracture, being 3.3 mkg./cm.² for specimens taken from the center and 6.2 to 13.8 mkg./cm.² for specimens cut from the outer portions of the weld. Although Rist believed his results showed that the effects of peening did not reach the inner portions of the weld-metal, there is a possibility that defects were present at the middle of the X. Rist did not mention chipping the root of the first layer. Stress relief had no effect, but normalizing raised the VGB value of the inner layers to over 13.3 mkg./cm.²

Widmannstätten structure was shown by Belaïew and Séférian[128] to be one of the chief causes of low notch impact value in welds. Oxyacetylene deposits (0.19 to 0.46 C, 0.3–0.9 Mn, up to 0.4 Si) in the as-welded condition with Widmannstätten structure had 10 ft.-lb. Charpy. A reverse run and annealing at 850° C. raised the Charpy value to 25–35 and 55–65 ft.-lb., respectively.

A comparison of the notch impact value of oxyacetylene and arc weld metals was made by Brillié and Roux,[129] using the standard French test methods. Deposits consisting of 18 to 20 layers of weld-metal each 0.04 inch thick were made with 0.16-inch rods, 500 liters acetylene per hr. Arc weld deposits were made in a trough of steel using 0.16-inch electrodes, 120–140 amp. D.C., electrodes negative. Annealing at 900° C. raised the Mesnager value of good arc and gas welds, quenching from 900° C. reduced the Mesnager value only if the carbon content of the deposit was 0.26% instead of 0.13%, or if the electrode coating was of poor quality. The same was true of deposits quenched from 600° C. and held 40 days at room temperature.

Welding Procedure.—Many of the effects of welding procedure on notch impact value have been pointed out in preceding sections. The gradual improvement in notch impact value of arc welds in the past few years has been shown by Stieler[131] in his survey of thousands of qualification tests on mild and low-alloy steel. There was no difference between A.C. and D.C., although Zschokke[113] found higher results with A.C. The following

DVMR values are averages derived from distribution curves and quoted by Stieler[130] for covered electrode welds.

Boiler plate (tensile strength = 48,000 lb./in.^{2}): 9 mkg./cm.^{2} unannealed
Boiler plate (tensile strength = 48,000 lb./in.^{2}): 10 mkg./cm.^{2} annealed
Mild structural steel (tensile strength = 53,000 lb./in.^{2}): 8 mkg./cm.^{2}
Low-Alloy steel (tensile strength = 74,000 lb./in.^{2}): 8 mkg./cm.^{2}

If low-grade electrodes were used in mild structural steel welds the DVMR value was usually 5 mkg./cm.^{2} The DVMR notch was in the root of the weld in all specimens.

As the current was increased from 450 to 550 amp., Polglaze[132] believed the Izod value of deposits made with $^3/_8$-inch covered electrodes decreased, although his test results offer little indication of an important effect exerted by welding current. The length of run deposited per electrode (10 to 30 inches) also had little influence on Izod value, the Izod value tended usually to increase 10 ft.-lb. or so as length of run was increased, unless the bead became so narrow that the deposit contained slag. The longer the length of bead per electrode, the more layers are required to build up a deposit (provided the width of groove remains the same) and the faster is the rate of cooling of each bead. Both factors contribute to grain refinement and Izod value.

The porous deposits did not always have low Izod value in Polglaze's tests. For example an "extremely porous" weld had 45–54 ft.-lb. Izod, whereas the highest individual value obtained by Polglaze for any deposit was 57 ft.-lb. On the other hand, relatively sound deposits made at slow welding speeds had only 23 ft.-lb. average. If the deposits were "extremely porous with slag" the Izod value was 10 to 20 ft.-lb. It appears that notch impact tests are not reliable indicators of holes or slag inclusions in welds despite the fact that the German standard notch impact test recommends a notch on the fusion line for the apparent purpose of detecting scarf defects. It was Harris'[30] experience that a low Izod value might be obtained from clean weld-metal and a high Izod from dirty weld-metal deposited presumably by the same type of rod. Swinden[133] also stated that fictitiously high Izod values may be obtained from weld-metal as a result of unsoundness. He emphasized the importance of inspecting the Izod fracture for defects. On the contrary, Rosenthal[134] found that one blow-hole in a weld may reduce the Charpy value from 47 to 33 ft.-lb. Several blow-holes reduced the Charpy value to 18 ft.-lb. which was still not so low as the Charpy value of "oxidized" deposits free from blow-holes. Polson and Sinfield[60] also stated that Izod variations in weld-metals are generally caused by holes and slag.

A thorough investigation convinced Quadflieg[72] that the DVMR notch impact value is raised by defects not located at the base of the notch, but is lowered if the defect lies at the base of the notch. For example, at −60° C. two specimens from the same arc weld had 0.47 and 1.01 mkg./cm.^{2}, the specimen with the lower value being free from defects, the specimen with the higher value revealing a defect in the middle of the fracture. In a set of specimens from another weld tested at −75° C. two specimens with defects had 0.73 and 3.09 mkg./cm.^{2}, the defects in the former specimen occurring at the base of the notch. Similar results were secured at temperatures up to 500° C. Quadflieg concluded that defects not located at the base of the notch give the crystals in their vicinity more room for deformation. Quadflieg's results on unnotched specimens for detecting poor penetration are summarized in the section on Other Notch Impact Specimens.

The Mesnager specimen was found by Eiche[135] to detect insufficient penetration in butt welds made in unscarfed mild steel plates 0.47 to 0.55-inch plates with covered electrodes 0.39 inch diameter. With a gap of 0.10 to 0.12 inch, 80 volts, 450–525 amp. A.C. with superposed high frequency the Mesnager value was 6 mkg./cm.^{2} Insufficient penetration (low current and small gap) yielded only 1.5 to 3.5 mkg./cm.^{2} Mesnager without affecting the tensile strength.

Quadflieg[136] found that the average of 12 Charpy tests on weld-metal from 4 covered electrodes of different diameters yielded:

Plate Thickness, Inch	Electrode Diameter, Inch	Charpy Value, Ft.-Lb.
0.48	0.16	32
0.72	0.24	35
2.2	0.28	28
0.96	0.32	33

Apart from the use of bare electrodes, there appears to be little to choose among the different welding processes from the standpoint of notch impact value, granted intelligent welding personnel. Lewis[137] and others[138] using oxyacetylene welding have had no difficulty in meeting Lloyds Izod impact specification of 20 ft.-lb. for all weld-metal. The values attained in the actual tests were in the vicinity of 50 ft.-lb. The Charpy value of double V oxyacetylene welds in boiler plate (tensile strength = 60,000–67,000 lb./in.^{2}, all-weld-metal) stress relieved 1 hr. at 650° C., tested by Plinke [139] was 22 to 32 ft.-lb.

For some obscure reason, Brillié[39] found that the Mesnager value of oxyacetylene weld-metal deposited by the same type of rod in different steels decreased from 17.2 to 11.6 mkg./cm.^{2} as the tensile strength of base metal increased from 57,000 to 85,000 lb./in.^{2} It is difficult to believe that Brillié's results have general significance in view of the fact that Houston[140] reported 33 to 49 ft.-lb. average Izod for all-weld-metal having a tensile strength of 88,000 lb./in.^{2} while Thompson[141] found 30 ft.-lb. Izod with notch in face of weld or in heat-affected zone of test plates attached to welded boilers, the tensile strength of which probably was about 60,000 lb./in.^{2} Polson and Sinfield's[60] statement that all-weld-metal regularly attains 70 to 80 ft.-lb. Izod appears a bit optimistic whereas Dorey's[142] experience that weld-metal invariably has higher notch impact value than common boiler plate is authoritative. Inglis[7] mentions butt welds in tubes 4 inches o.d., 1-inch wall, that had 57 ft.-lb. Izod at room temperature, 43 ft.-lb. at −30° C.

Carbon arc welds made in a shielded atmosphere, according to Jennings[106] had 30 to 45 ft.-lb. (type of specimen not stated). Without a shielding atmosphere carbon arc welds attain only 2 to 8 ft.-lb. The Izod value of Unionmelt welds in ship steel (0.20 C, 0.4 Mn, $^1/_2$, $^3/_4$ and 1 inch thick) was found by Jackson[68] to be 29 to 48 ft.-lb. (average = 36 ft.-lb.). It appears that a reverse run was used in the welds, at any rate in the 1-inch plate. Automatic arc welds in plain carbon steel have over 29 ft.-lb. Charpy, according to Sarazin.[143]

Resistance Welds.—Flash welds were made by Kilger[144] in open-hearth steel bars 1.18 inches diameter (0.12 C, 0.50 Mn, 10.8 mkg./cm.^{2} DVMR core, 11.2 mkg./cm.^{2} outer layers). To test the welds Kilger used a non-

standard specimen $2^1/_2$ inches long, 10 mm. diameter, notch 5 mm. deep, 0.65 mm. radius. With welding times of 62–83 sec. (preheat 40–60 sec., flash 15–30 sec., reheat 1–3 sec.), loss by flash 0.39–0.55 inch, peak load 65–70 kw., the notch impact value increased as upset pressure increased from 2 mkg./cm.^{2} at 440 lb./in.^{2} to 7 mkg./cm.^{2} at 4200 lb./in.^{2} With 95–115 kw. (total welding time 34–53 sec., loss by flash 0.6–0.9 inch) there was less dependence on pressure in the same range than with lower power but the notch impact value was about the same. The variations in notch impact value were due more to slag than to microstructure, except with high pressures in which case the forging action improved the structure.

Non-Ferrous Welds.—Oxyacetylene welds in soft and hard-rolled aluminum (99.5% purity, 0.39 and 0.79 inch thick) made by Zimmermann[145] with aluminum rods of commercial purity had about one-half as high notch impact value (DVMR as well as VGB) as unwelded base metal. Cold hammering improved the notch impact value 1 to 4 mkg./cm.^{2}, and hot hammering raised the value to that of unwelded base metal. Hammering had little effect on the static tensile strength and ductility. Apparently the hammering consisted of leveling the reinforcement after welding had been completed. If so, the effect must have been confined to the outer layers of the weld and consisted of grain refinement, together with perhaps some closing of porosity. The notch was in the root of the welds in 0.39-inch plate and DVMR specimens were used. Hammering had more effect on the VGB specimens (with notch perpendicular to surface of plate) that were taken from the 0.79-inch plates than on the DVMR specimens cut from the thinner plates.

The Charpy value of bars of solder has been determined by Gonser and Heath,[146] and ranged from 10 to 20 ft.-lb. for different compositions. The highest Charpy value of lead-tin solders was obtained with 40% tin. Silver had a good effect; copper a bad effect. Presumably the higher the Charpy value of the solder, the less likely is the soldered joint to crack suddenly in service.

Flame Cutting.—The notch impact value of flame cut surfaces has been determined by three investigators who agree that, for the steels tested and with the specimens employed, flame cutting does not have an adverse effect. Zimmerman[147] tested sheared, machined, friction sawed, and manual and automatic oxyacetylene flame cut surfaces in a steel $^1/_2$ inch thick containing 0.24 C, 0.46 Mn. With a Charpy notch perpendicular to the cut edge, so that one end of the notch extended to the cut surface, it was found that machine flame cuts (20 to 22 ft.-lb.) were as good as machined surfaces and superior to sheared and friction sawed. Manual flame cuts (31 to 38 ft.-lb.) were superior because the heat effect was extensive. For the same reason the area of the cut surface on the torch side had slightly higher Charpy value than the under surface.

In Hessler's[148] tests on machine flame cut steels 0.79 inch thick containing 0.10 C, 0.44 Mn or 0.18 C, 0.47 Mn, VGB specimens in the latter steel showed that milled surfaces had only one-half the notch impact value of flame cut surfaces, the notch being parallel to the cut surface. Oxyhydrogen cuts were superior to oxyacetylene. With DVMR specimens of the lower carbon steel, the machined surface had the same notch impact value (10.6 mkg./cm.^{2}) as the average of specimens cut from the torch and lower sides of the flame cut surface. Specimens from the torch side had 13 mkg./cm.^{2}, specimens from the lower side 8 mkg./cm.^{2}

Machine flame cuts in steels containing 0.12 C, 1.0 Mn, 0.6 Si, with 0.3 Cu or 0.4 Cu and 0.1 Mo were tested by Dümpelmann[149] using DVMR specimens and plates 0.39 inch thick. Whether the notch was parallel or perpendicular to the flame cut surface the DVMR value was 7 to 8 mkg./cm.^{2} with the notch parallel to the direction of rolling and 11 to 15 mkg./cm.^{2} with notch perpendicular to the direction of rolling. From the standpoint of notch impact value there was no essential difference among acetylene, hydrogen and city gas for preheating. The bend test appeared to be more sensitive than the notch impact test in detecting differences between flame cut surfaces in these steels. Perhaps tensile impact tests or tests of minute specimens cut from the flame cut specimens might provide interesting information. Tests on the low-alloy higher carbon steels would also be important.

That welds in scarves prepared by oxyacetylene flame cutting have the same Izod value as with machined scarves was demonstrated by Daniel and Durrant.[150] If the edges are flame cut without scarf angle and the weld is made with $^3/_{16}$-inch gap (plate 0.40 inch thick), Dobson[151] found that the Izod value was only 6 to 32 ft.-lb., oxyacetylene cut scarves being a little better than coal gas cut scarves. For comparison the unwelded base metal had 45 ft.-lb. Izod and 60° V butt welds had 23 to 36 ft.-lb. Izod. Unquestionably, the low values for the welds made without scarf angle was not due to their having been flame cut.

REPEATED IMPACT

The interpretation of repeated impact tests with notched specimens is so extraordinarily difficult that the test has been abandoned by practically all welding investigators. Kilger[144] used the Krupp machine with specimens 0.59 inch diameter notched to 0.53 inch at the weld and rotated 180° after every blow in his investigation of flash welds in mild steel bars 1.18 inches diameter. The number of blows to fracture increased in an unsteady fashion from 2000 to 8000 at the upset pressure corresponding to the elimination of visible defects from the weld. Thom[152] states that he is investigating the relationship of Izod to repeated impact value of weld-metal.

Allowance for repeated impact delivered by reciprocating machinery or falling weights in the design of welded steel buildings, Newport[153] found, is unnecessary except in small stiff secondary beams subjected to considerable impact. The ends of unusually stiff secondary beams having small static deflection (0.01 inch) should be designed for flexibility to reduce the bending stresses in the vicinity of the welds. The stress due to repeated impact is largest at first mode (fundamental) resonance than at higher modes.

WELDED STRUCTURES

It was shown in the earlier review of literature on Impact Tests for Welded Joints that welded structures of all sorts could be relied upon for excellent resistance to impact in service. By impact in service is meant not alone the repeated hammer blow received by a welded rail joint from passing locomotives but also the unexpected sudden blow of unusual intensity under which the weld must hold to prevent widespread damage. Since the earlier review was prepared, no evidence has been offered to show that welds are becoming less resistant to impact in service. It is extremely doubtful whether there is anything to the statement[154] that "a welded joint whose length is parallel to the direction of stress resists impact much better than one at right angles."

The railways often apply impact tests to welded structures. According to Muller,[155] impact and bend tests are the most commonly used tests for examining individual rail joint welds. The German Railways make impact tests from time to time to make sure the work is being done properly. Hopkins[156] made a plea for routine hammer testing fusion welded pressure vessels or models thereof, but there is little likelihood of the practice becoming generally established. Melhardt,[87] Golling and Tulacz,[157] and Michaud[158] report excellent drop impact test results on oxyacetylene welded rail joints of different types, and Otto[159] and the Sperry Rail Service[160] for flash-welded joints.

Arc-welded joints in two types of bullhead rails (0.41 C, 0.75 Mn and 0.53 C, 1.05 Mn) had extremely variable drop impact value in tests by Csilléry and Péter.[173] The rail head was butt welded with a bare electrode (0.60 C, 0.5 Mn in deposit, 4.1 mkg./cm.^{2} DVMR), a foot strap (0.04 C) being fillet welded only to the web with an intermediate insert by means of a covered electrode (0.08 C, 0.18 Mn in deposit, 12.68 mkg./cm.^{2} DVMR). In the interests of ductility the tension zone at the foot was not welded. Nevertheless, failure occurred in several joints at 2 or 3 blows, whereas others withstood 8 or 9 blows (1100 lb. falling 6 feet) without failure. The rail joints in the softer steel failed outside the foot strap, fracture extending through the bare electrode welded head. In the stronger rail failure originated in the fillet welds or welded inserts.

Buffing tests of welded German freight cars[161] revealed failure of a fillet weld at 13 miles per hr. in an experimental design. On account of their small volume, fillet welds not properly carried around the ends of a member have low capacity for deformation in impact. Butt welds also failed in some tests, the causes being notch effect and bending. Fillet welds of the relieving type and diagonal stiffeners remedied both difficulties. Henderson[162] reports no damage to the welds in a shunting test of an arc-welded British railway coach underframe at 9$^1/_2$ miles per hr. An arc-welded mild steel freight car underframe[163] survived a buffing test at 15 miles per hr. without failure. At 35 miles per hr. the frame was severely distorted but no welds failed.

The buffing tests of an experimental box car of low-alloy steel (22% arc welded, 54% spot welded, 24% riveted) made by the Association of American Railroads[174] at speeds up to 16 miles per hour showed that the design of the welds and car was faulty. Both spot and fillet welds failed at relatively light impacts, the percentage of failures due to improper welding technique not being determined.

An interesting analysis of the damage to a warship with welded bulkheads struck by a mine has been made by Stanley.[172] Under the impact of the explosion "the welding showed itself to be sufficiently flexible to take up distortion which, in a riveted structure, would have caused the rivets to pull through or break." An oval 150-gallon tank made of 14 gage, 2% Cu—1% Ni structural steel welded with covered electrodes was filled with water and dropped[164] on a heavy metal plate. Although the tank landed on one corner, no welds failed and the tank remained tight. The test was repeated with equally encouraging results. Johnson[165] cites a fusion welded magnesium alloy spherical gondola of a stratosphere balloon that was shattered by a free fall from a height of 3000 feet. There were practically no failures in the welded seams.

An unusual instance of impact resistance was provided by a 12-inch box section formed by welding[166] two standard 12-inch channels together with plates 12 inches wide, $^1/_2$ inch thick. Two-pass, $^1/_2$-inch fillet welds were made. The box girder was placed on railway ties 8 ft. apart. An 1800 lb. weight was dropped on the section 100 times starting from a height of 3 ft. and increasing the weight steadily to 75 ft. The welds held. Brazed sub-assemblies for business machines, according to Webber,[167] seem to withstand repeated impact better than pinned or riveted joints. In Kuhlmann's[168] tests a lever copper brazed in a protecting atmosphere withstood nearly 10 million blows and fractured outside the joint. A similar pinned joint failed in 4$^1/_2$ million cycles.

Drop tests on two arc-welded end seats of short span girders are described by Risdon.[169] Under terrific punishment the seats failed in the web and continued into a weld. When the welded tubular KDKA antenna tower[170] weighing 50 tons fell, on account of a faulty guy wire when construction had reached a height of 640 ft., no defects were detected in any of the 3800 welds, which were made with heavily fluxed electrodes. Drop tests on arc-welded oil well casing 7 inches o.d., $^9/_{32}$-inch wall by Hodge and Sadler[171] showed that the joints were tough.

SUGGESTED RESEARCH PROBLEMS

It is believed that the research problems on impact testing of welds prepared by Henry[18] and discussed by a number of experts will remain in force for some time to come.

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TESTS OF WELDS IN ALLOY STEELS

Hardness and Impact Strength of Critical Sections in Welded Steels*

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Photography and Metallography of Welds*

By F. C. FAIR‡

THIS research was conducted in the Materials Testing Laboratory of the Department of Civil Engineering, New York University.

The main purpose of investigation of impact strength and hardness of four kinds of steel alloy butt welded with two different types of electrodes may be briefly summarized as follows:

1. To determine relative hardness and impact strength of weld, heat-affected zones and base metal;
2. To ascertain if a definite relationship exists between the hardness and impact strength of various critical sections;
3. To find out the effect of heat treatment on weld and heat-affected zones;
4. To study the relative merit of two kinds of electrodes.

Eight plates, 1/2 inch thick, were furnished for the preparation of specimens. Their chemical composition and the types of electrodes employed are given in Table 1. Each weld was made of four layers, using a 3/16-inch electrode, and a seal bead had been placed at the back of the joint. The joints were beveled at 30 degrees, thus making a single 60° V. Stress-relieving temperature was 6 hours at 600° C.

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Table 1

Plate No.	Chemical Composition	Electrode	Heat Treatment
1	3.50% Ni; 0.35% C	Electrode A containing 1% Ni: 0.5 Mo	None
3	3.50% Ni; 0.35% C	Electrode A containing 1% Ni: 0.5 Mo	Stress relieved
5	2.75% Ni; 0.22% C; 0.30% Mo	Electrode A containing 1% Ni: 0.5 Mo	None
7	2.75% Ni; 0.22% C; 0.30% Mo	Electrode A containing 1% Ni: 0.5 Mo	Stress relieved
9	0.16% C; 0.60% Mn; 0.25% Si; 2.09% Ni; 1.06% Cu	Electrode B	None
10	0.16% C; 0.60% Mn; 0.25% Si; 2.09% Ni; 1.06% Cu	Electrode B	Stress relieved
13	0.23% C; 0.72% Mn; 0.02% Si; 0.79% Ni; 1.56% Cu; 0.15% Mo	Electrode B	None
14	0.23% C; 0.72% Mn; 0.02% Si; 0.79% Ni; 1.56% Cu; 0.15% Mo	Electrode B	Stress relieved

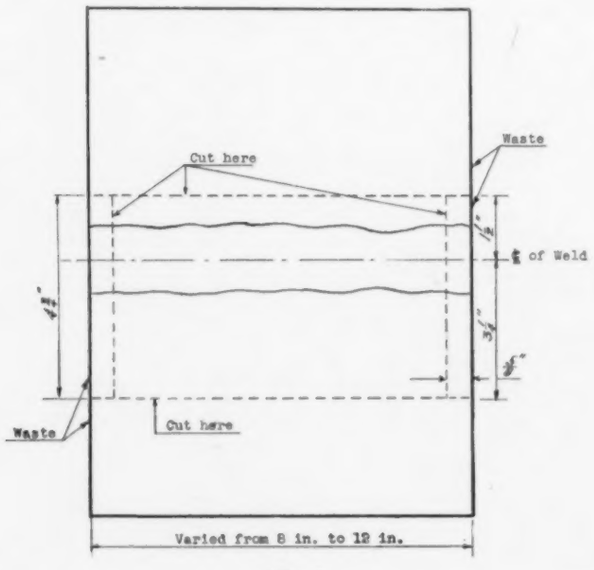


Fig. 1

PREPARATION OF IMPACT SPECIMENS

All plates were straight, so that no flattening was necessary. After the edges of plate were cut as shown in Fig. 1, each face was machined in equal amounts so as to reduce the thickness to about 12 mm. The plate then was sliced into strips, 12 mm. wide, by means of four slitting saws which were operated at slow speed and supplied with ample lubrication. Plates No. 5 and No. 7 were hardest to saw and face (but not to slice) because of their surface toughness, although their Brinell hardness numbers were 270 and 255, respectively. Before the strips were removed from a special vise, each strip was stamped with an identification number. All sides of each strip were ground in a magnetic chuck to within 0.002 inch of the required dimensions (10 mm. x 10 mm.). In order to bring out more clearly the lines of

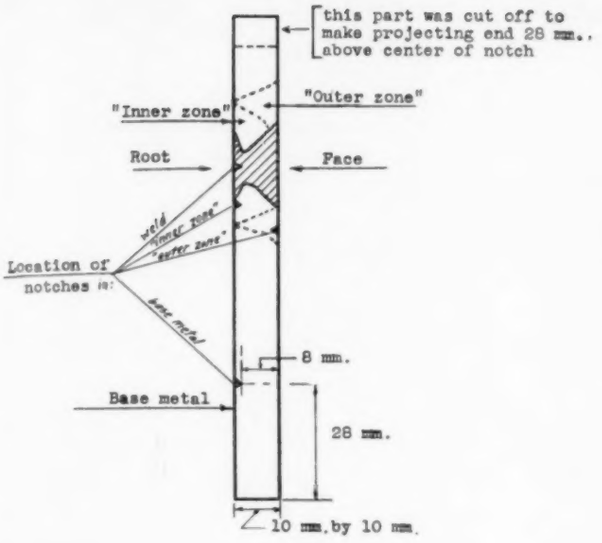


Fig. 2

demarcation of heat-affected zones, particularly in non-stress relieved specimens, the test pieces received a final polishing with Hubert papers, No. 1 and No. 0. The specimens were etched in a solution of five per cent nitric acid in alcohol and the boundaries of welds and heat-affected zones were traced with black India ink. A typical example of a specimen exhibiting more than one heat-affected zone is that of Plate No. 5 as shown in Fig. 8.

Because of the original beveled edge in weld and irregularities of lines of fusion and of various heat-affected zones, it was decided to set notches in two sections of the heat-affected zone and identify those sections as "inner zone" which is closer to weld, and "outer zone" which is closer to base metal (see Fig. 2). Although the "outer" heat-affected zones were absent in stress-relieved specimens, the notches were set approximately where the "outer zones" existed in non-stress relieved plates.

Notches were 45° and of 0.25 mm. root radius. The thickness of critical sections was 8 mm. All projecting ends above the center of notches were 28 mm. 106 specimens were provided for impact strength investigation and 16 were set aside for hardness tests and microscopic analysis.

IMPACT TESTING

Impact tests were performed in a standard Izod impact machine of 120 ft.-lb. capacity, about two weeks after the specimens were cut into strips from plates. There was no warping of specimens. After the welds and heat-affected zones were tested, the undamaged ends of specimens were notched in the base metal. Altogether there were 169 impact tests made. The results are given in Table 2. The graphical chart of impact strength of various sections of plates, affording a rapid visual analysis, is presented in Fig. 5.

HARDNESS TESTS

The exploration of hardness was made on a Rockwell hardness machine, using a diamond penetrator with 150 kgs. load. Two untested impact specimens of each plate were subjected to hardness tests on all of their four sides. In order to avoid erroneous readings, the clearance between the test points was kept within 0.08 inch. Typical examples of hardness distribution for all plates are shown in Figs. 6 and 7. These are the tracings obtained by projecting the four sides of each specimen on a screen of velum paper by means of a delineascope. The tabulated average hardness and variation of hardness for all plates are given in Table 3.

DISCUSSION OF RESULTS

Izod Impact Tests

Photographs in Figs. 3 and 4 show typical specimens after impact tests. The weakest sections in impact strength of all plates were at the welds. A considerable difference in strength in welds was observed between Type A and Type B electrode. The average strength was as follows:

Table 2

IMPACT STRENGTH IN FT.-LBS. (IZOD)

Plate No.	Average				variation max. - min.			
	weld	inner zone	outer zone	metal	weld	inner zone	outer zone	metal
1	33.8	58.3	75.5	79.5	35.0-32.5	62.0 - 56.5	81.0 - 65.0	84.0 - 66.0
3 (heat treat.)	34.8	82.1	96.6	92.5	41.0- 27.5	104.5 - 75.0	104.0 - 89.0	106.3 - 80.0
5	29.6	60.9	114.8	93.8	34.0- 29.0	67.0 - 47.5	117.0 - 96.8	111.0 - 84.0
7 (heat treat.)	34.5	79.8	92.2	92.5	36.0- 33.0	88.0 - 72.5	100.5 - 87.5	105.5 - 84.5
9	57.1	83.9	85.9	88.6	60.5- 39.0	91.0 - 79.3	90.0 - 81.5	91.0 - 87.3
10 (heat treat.)	61.6	93.5	94.6	82.9	70.5- 54.0	105.0 - 86.5	98.8 - 92.5	86.5 - 78.5
13	49.8	105.2	86.7	67.7	51.5- 47.5	106.5 - 93.5	102.5 - 87.0	80.0 - 39.5
14 (heat treat,)	59.8	108.4	102.5	87.2	66.0- 41.5	111.5 -104.0	108.5 - 82.0	89.0 - 83.5

Table 3

ROCKWELL HARDNESS (C - scale)

Plate No.	Average				variation max. - min.			
	weld	inner zone	outer zone	metal	weld	inner zone	outer zone	metal
1	9	20	13	14	18 - 5	27 - 18	26 - 17	16 - 10
3 (heat treat.)	13	12	10	11	14 - 4	14	12	11 - 9
5	19	25	23	20	10 - 8	35 - 25	27 - 20	21 - 19
7 (heat treat.)	13	16	16	14	14 - 6	18 - 15	18 - 15	16 - 14
9	8	14	11	8	13 - 0	16 - 8	13 - 7	11 - 8
10 (heat treat,)	10	8	6	6	13 - 2	13 - 6	8 - 6	7 - 4
13	?	16	16	16	12 - -5	18 - 15	18 - 12	18 - 16
14 (heat treat.)	6	13	11	9	12 - -2	15 - 7	11	10

Type A Electrode

For non-stress relieved welds 31.7 ft.-lb.

For stress-relieved welds 34.6 ft.-lb. (an increase of $9^1/_2$%)

Type B Electrode

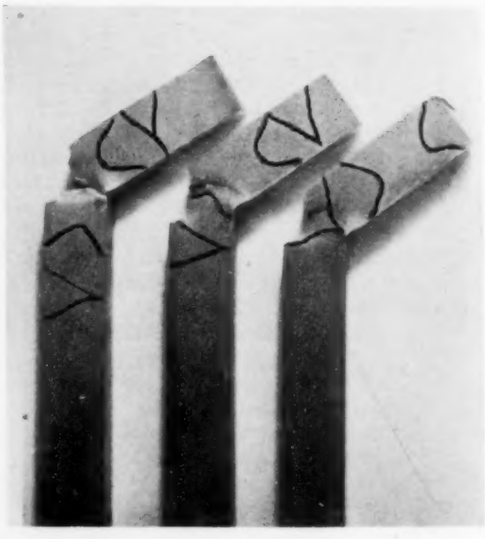
For non-stress relieved welds 53.5 ft.-lb.

For stress-relieved welds 60.7 ft.-lb. (an increase of $13^1/_2$%)

Heat treatment improved the strength of welds by about 10%. Type B electrode showed a definite superiority over Type A, giving about 50% higher values in impact strength than the latter which is undoubtedly due in part to the difference in alloys welded.

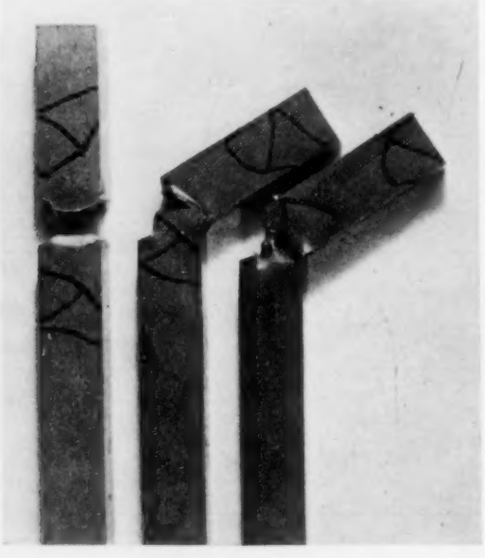
Plates No. 1 and No. 5 showed their welds to be brittle. Heat treatment somewhat removed the brittleness in Plate No. 5, but had no effect on Plate No. 1.

The impact strength of welds in Plates No. 3, No. 9 and No. 14 was not affected by the location of the notch whether on the face or on the back of the welds.



W. I.Z. O.Z.

Fig. 4—Plate No. 9



W. I.Z. O.Z.

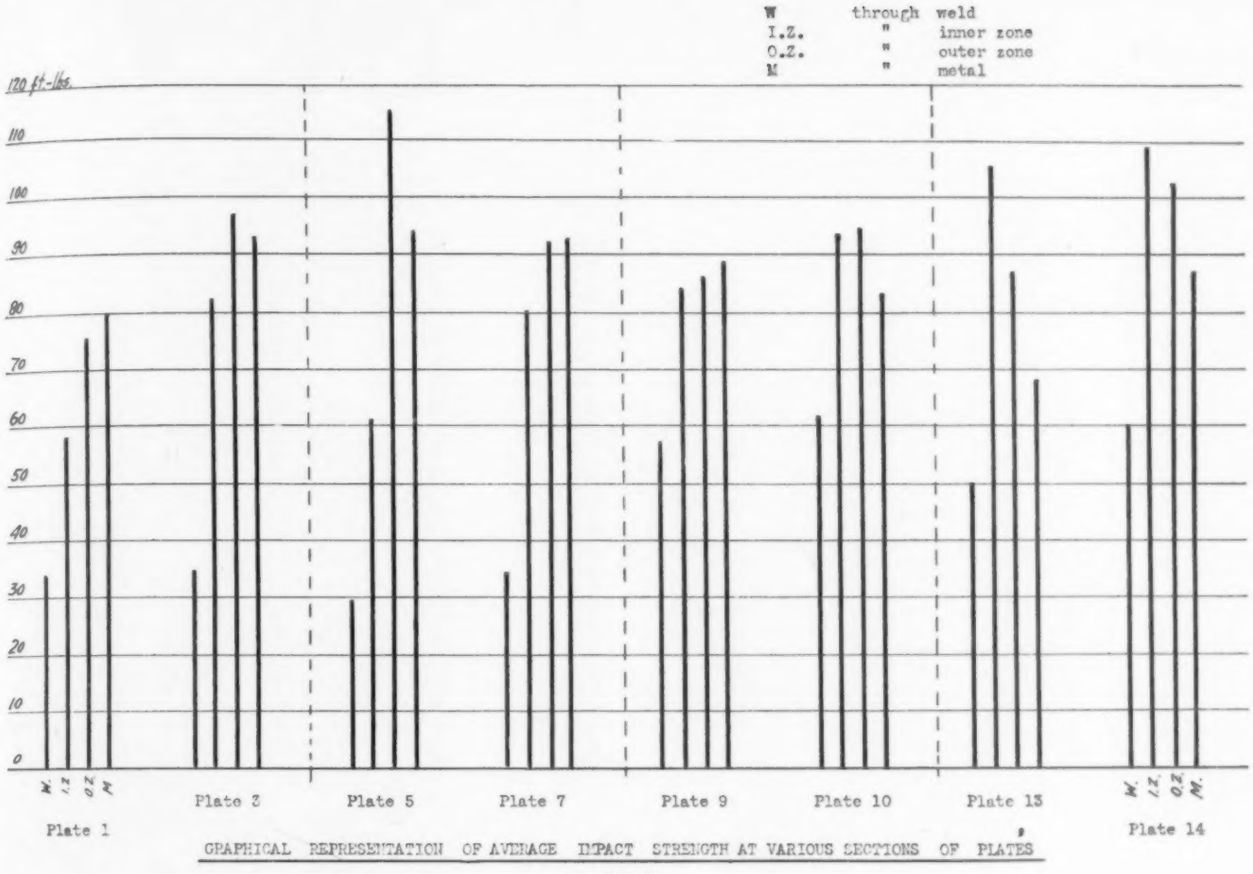
Fig. 3—Plate No. 5

Next to welds, the "inner" heat-affected zones were most critical. The comparison of the impact strength of "inner" zones with that of the base metal is given in Table 4.

In Plates No. 1, No. 3, No 5 and No. 7 where Type A electrode was used the "inner" heat-affected zones showed average impact strength of 79% of that of the base metal. In Plates No. 10, No. 13 and No. 14 where Type B electrode was employed, the "inner" zones gave average impact strength of 122% of that of the base metal.

The strongest "inner zones" were in Plates No. 13 and No. 14. These zones were about 50% stronger than the welds.

It is apparent that the strength of "inner" heat-affected zones is a function of the type of electrode and the steels welded. In some welded steels, their strength ex-



GRAPHICAL REPRESENTATION OF AVERAGE IMPACT STRENGTH AT VARIOUS SECTIONS OF PLATES

Fig. 5

Table 4

Plate	Base Metal	Stronger or Weaker	Than "Inner" Zone	By Per Cent
1	Base Metal	Stronger	Than "Inner" Zone	23
3	Base Metal	Stronger	Than "Inner" Zone	11
5	Base Metal	Stronger	Than "Inner" Zone	35
7	Base Metal	Stronger	Than "Inner" Zone	14
9	Base Metal	Stronger	Than "Inner" Zone	5
10	Base Metal	Weaker	Than "Inner" Zone	12
13	Base Metal	Weaker	Than "Inner" Zone	55
14	Base Metal	Weaker	Than "Inner" Zone	24

ceeded that of the base metal by as much as 35% or were weaker by as much as 55%.

The "outer" heat-affected zone exhibited the same strength as the base metal in Plates No. 1, No. 3, No. 7 and No. 9. None of these plates was subjected to heat treatment. While in Plates No. 5, No. 13 and No. 14, the "outer" heat-affected zone was about 20% stronger than the base metal, Plate No. 5 gave the strongest "outer zone" of 115 ft.-lb.

In general, heat treatment somewhat improved the impact strength of all sections in all plates, except Plate No. 5 in which the "outer zone" showed a decrease of about 20% after heat treatment.

Considerable variation in impact strength was observed in the various sections of each plate as shown in Table 5.

Table 5—Per Cent Variation in Impact Strength

Plate No.	Section	% Variation	Number of Specimens Tested
13	Base metal	36	10
3	Inner zone	21	5
3	Weld	21	5
9	Weld	28	6
14	Weld	27	7

Manner of Failure

Typical failures in impact tests of specimens of all plates are shown in Figs. 3 and 4, which are photographs taken after tests.

Welds of Plates No. 1, No. 3 and No. 5, showing low-impact values, fractured along sections perpendicular to the axes of test pieces. This type of failure can be classified primarily as tension failure. The fractured sections of all welds exhibited silky to fine grain structure.

The line of fracture in the majority of specimens, where the notch was set in heat-affected zones, extended from the root of the notch upward in the direction of the neutral axis of the test piece. This type of fracture can be attributed to the shearing stresses along the fiber of the material. The fractured sections in all cases showed fibrous structure. The most irregular fractures occurred in the heat-affected zones of Plates No. 5 and No. 13. In a very few cases the fractures had a tendency to follow the lines of fusion.

The examination of fractured sections by the naked eye reveal the absence of slag, blow-holes or other defects in the material.

Rockwell Hardness Tests

The faces of all welds were about twice as soft as the backs. The softest face of weld was in Plate

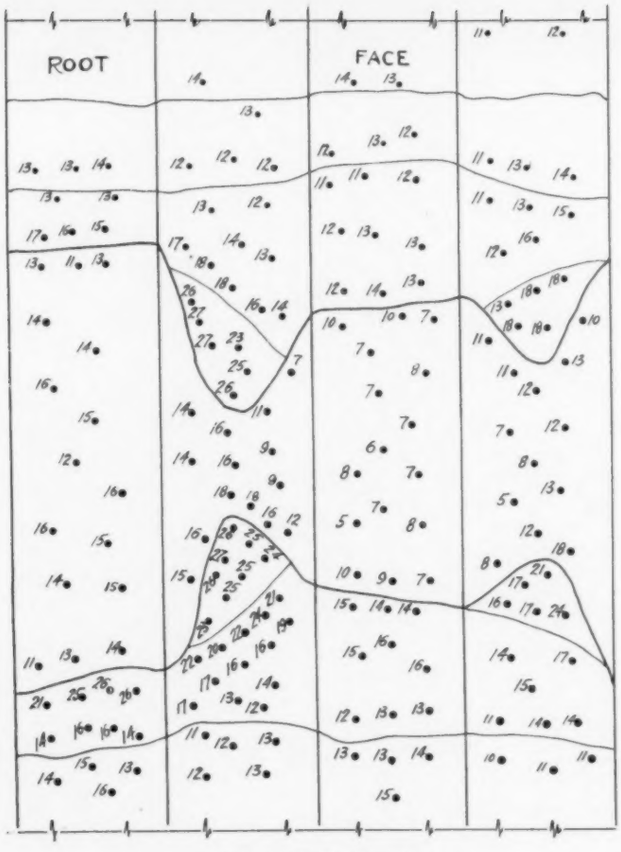


Fig. 6—Plate No. Sp. No. 2

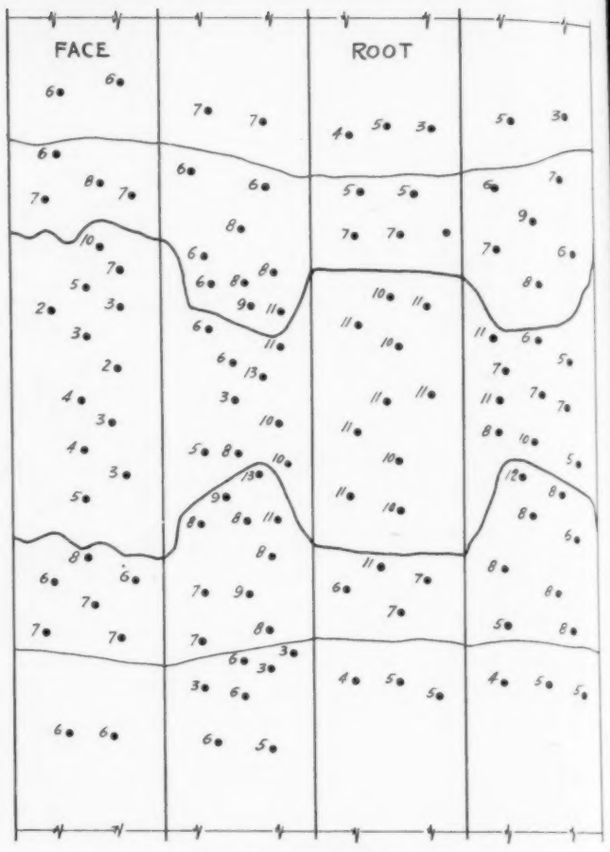


Fig. 7—Plate No. 20 Sp. No. 22

No. 13 (— 5C). The "inner zones" proved to be the hardest of all sections, particularly in Plate No. 5 (35 C) and Plate No. 1 (27 C). Heat treatment softened these hardest zones so that they and the "outer zones" exhibited more or less the same hardness. The hardness of all sections in Plate No. 3 was reduced about 50% after heat treatment.

It is difficult to predict the exact effect of this particular heat treatment (6 hours, 600° C.) on the hardness of various critical sections because it was physically impossible to test the very same specimens before and after heat treatment. Generally, however, the tests indicate that heat treatment softened the welds, heat-affected zones and base metals, but hardened the softest regions of faces of welds in the case of Plate No. 13.

Metallography of Welds

Specimens from some of the plates were prepared for microscopic examination. These, in general, included the region of the weld. Most of the specimens were found to be entirely regular in appearance. A detailed report on the examination of these specimens seemed to be outside of the scope of this paper. Two samples

Fig. 8

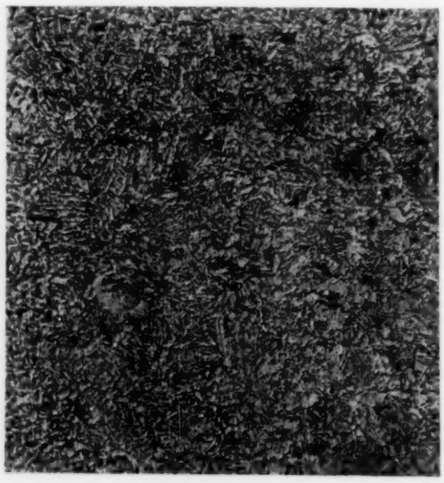


Fig. 9—Region 1

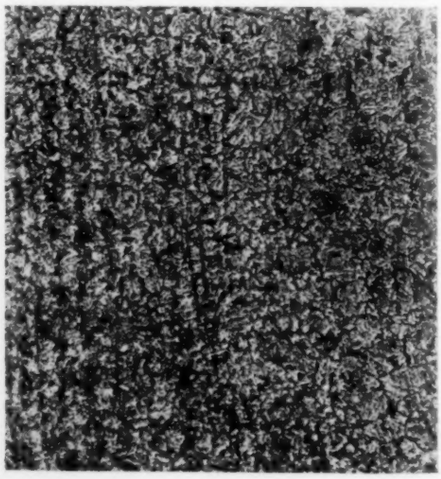


Fig. 10—Region 2

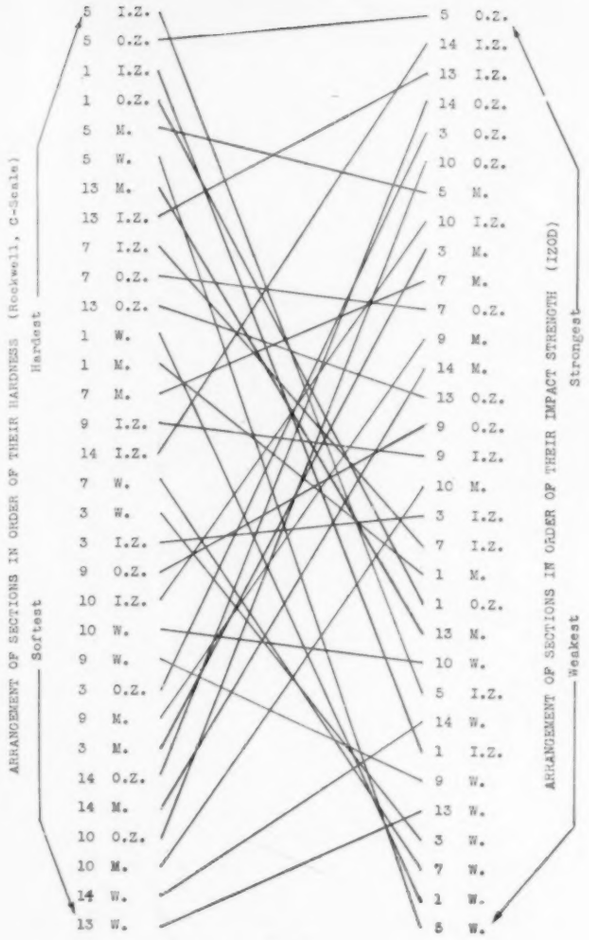


Fig. 11

showed on the surface blow-holes about 0.1 mm. in diameter. The surfaces of these holes were clean and the immediately adjacent metal exhibited the same constitution as the remainder of the metal in that vicinity. Plate No. 5 showed such irregularities in hardness that a more thorough examination was made of this metal. Figure 9 shows the microstructure (100×) of the weld-metal at the center of the top of the weld where the Rockwell hardness was about ten. Figure 10, (100×) was taken at the junction of the weld and parent metal where the hardness varied in a short space from 14 to 21. Figure 8 shows the macroetch of this specimen. The location of Figs. 9 and 10 are indicated on this photograph.

Relation Between Hardness and Impact Strength

In Fig. 11 the sections of all specimens are arranged in order of their hardness and impact strength in a manner to give a visual picture of relationship between hardness and impact values. Examination of this chart affords the conclusion that no exact relation between hardness and impact can be established. In the cases such as,

Outer zone	of Plate	No. 5
" "	"	No. 7
Inner zone	"	No. 13
Weld	"	No. 9
"	"	No. 10
"	"	No. 13
"	"	No. 14

the order of hardness is definitely the same as that of impact strength. While in the cases of

Base metal	of Plate	No. 9
Outer zone	"	No. 1
" "	"	No. 9
" "	"	No. 10
" "	"	No. 14
Inner zone	"	No. 1
" "	"	No. 3
" "	"	No. 5
" "	"	No. 9
Weld	"	No. 5

the impact strength varied inversely to the hardness.

It is outside of the scope of this paper to discuss the kind, manner and complexity of stress distribution in a region of a material subjected to hardness and impact tests. However, this much may be said that since the hardness property of a material is a complex one and is usually defined in an arbitrary fashion, it is doubtful whether it can be expressed as some function of another property, also arbitrarily defined, such as shock absorbing characteristics. Only an approximation between hardness and impact values can be made if such distinct properties as cohesive or shearing strength of crystalline structures have been equally emphasized in both of these arbitrary tests.

GENERAL CONCLUSION

1. (*a*) Weakest sections in impact strength were in welds; their average impact strength was about 40% for alloy steels welded with Type A electrode and 70% for alloys welded with Type B electrode of their respective base metals.

(*b*) Next to welds, the "inner" heat-affected zones (regions close to welds) were the most critical in Plates No. 1, No. 3, No. 5 and No. 7 where Type A electrode was used. These sections showed average impact strength of 79% of that of the base metal. In Plates No. 9, No. 10, No. 13 and No. 14, where Type B electrode was used, the "inner" heat-affected zones gave average impact strength of 122% of that of the base metal. In other words, the impact strength of "inner" heat-affected zones is a function primarily of the kind of electrode used; in some welded steels their strength exceeded that of the base metal by as much as 35% or were weaker by as much as 55%.

(*c*) The "outer" heat-affected zones (regions away from welds) proved to be stronger than the base metal by about 9%.

2. A definite relationship cannot be established between hardness and impact strength for different sections of welded steels.

3. Heat treatment improved somewhat the impact strength of heat-affected zones as well as the welds. The average value of impact strength of heat-treated plates was 117% of that shown by non-stress relieved plates.

4. Impact strength of welds is determined primarily by the type of steel and electrode used. Type A electrode showed a superiority in impact strength of welds of 50% over Type B electrode for the alloy steels used.

ACKNOWLEDGMENTS

The authors wish to express their appreciation to Mr. W. Spraragen and the Welding Research Committee who made this research possible, to Dr. C. E. Claussen for his interest and suggestions in this research, to Dr. H. E.

Wessman for his helpful criticism of this paper and to Mr. W. Pritchard for his painstaking care in the preparation of specimens.

The generosity of The Watertown Arsenal in supplying the welded steel plates for this research is hereby acknowledged.

PHYSICAL and METALLOGRAPHICAL

Investigation of Low-Carbon Low-Alloy Welded Steel*

By ROBERT C. BOEHM†

FATIGUE TESTS

THE material used in the fatigue tests was hot-rolled copper-nickel alloy steel plate (Yoloy). The specified analysis of the steel is as follows: 0.21% carbon, 0.80% manganese, 0.027% sulphur, 0.009% phosphorus, 0.17% silicon, 1.80% nickel and 0.90% copper.

The original Yoloy plate was approximately 14 inches wide, 30 inches long and $^3/_4$ inch thick. A section 10 by 14 inches was cut off the plate to be used for making the parent metal test specimens. The remaining plate was then cut in half longitudinally. All cuts were made by means of an oxyacetylene torch.

The two sections of the plate which had been cut in half longitudinally were then welded together using a single vee butt type weld with a reinforcing bead on the under side. The plate was welded at the Wellman Engineering Company's plant under the direction of Mr. Williams using a high grade coated electrode.

After the plates had been welded together, both the "as-received" plate and the welded plate were cut into sections having a cross section of $^7/_8$ by $^3/_4$ inches. These bars were cut with a cutting torch. It is assumed that the hardening effect of the torch would have no effect on the internal metal (metal to be used for the tests), since more than an $^1/_8$ inch of the external metal is cut away on machining.

After the specimens had been cut they were divided into three groups for thermal treatment. The first group received no heat treatment and was tested in the as-received condition for both the parent and weld-metal. The second group was stress relieved. This consists in heating the test bars up to 1250° F. and furnace cooling. The third group was normalized. This consists in heating the test bars up to 1650° F. and furnace cooling. These groups will hereafter be referred to a the as-received, stress-relieved and normalized groups, respectively.

The fatigue specimens were of the R. R. Moore rotating bend type.

The Yoloy steel in the "as-received" condition had an endurance limit of 47,000 lb. per sq. in. as compared to an endurance limit of 28,000 lb. per sq. in. for a plain carbon steel having the same carbon content.

An endurance limit of 42,000 lb. per sq. in. was obtained after subjecting the Yoloy steel to a stress relief of 1250° F. This is a drop of 10.6% from the "as-received" condition. After normalization at 1650° F., this steel had an endurance limit of 38,000 lb. per sq. in.

The endurance limit of the weld, Fig. 1, in both the "as-welded" and stress-relieved conditions was 30,000 lb. per sq. in. when welded with coated rod. The endurance limit of the weld in the normalized condition was 28,000 lb. per sq. in. when welded with the same rod. The weld in the stress-relieved condition did not change in structure from that of the "as-welded" condition, but when normalized the structure was enlarged. This accounts for the very slight drop in fatigue strength after normalization. The values of endurance limit obtained for these welded structures correspond closely to values obtained in welded structures by other parties. While the fatigue strength of the weld-metal is considerably lower than that of the base metal, this is not alarming as it is a common occurrence in welded steels.

IMPACT TESTS

The materials used in the Charpy impact test were the low-alloy steels—Yoloy, Cor-ten and Man-ten. The specified analysis of these steels are as follows: (1) Yoloy—0.18–0.23% carbon, 0.60–0.90% manganese, 2.0% nickel minimum and 1.0% copper minimum; (2) Cor-ten—0.10% carbon maximum, 0.10–0.30% manganese, 0.10–0.20% phosphorus, 0.05% sulphur maximum, 0.50–1.00% silicon, 0.30–0.50% copper and 0.50–1.50% chromium; (3) Man-ten—0.35% carbon maximum, 1.25–1.70% manganese, 0.04% phosphorus maximum, 0.05% sulphur maximum, 0.15% silicon minimum and 0.20% copper minimum.

The alloy steel plates were cut in half and then welded together using a single vee weld with a reinforcing bead on the closed side. The plates were welded with shielded arc electrodes.

After the plates had been welded they were machined and then divided into three groups for thermal treatments. The heat treatments given the Charpy specimens are similar to those given the fatigue specimens—stress relieving at 1250° F., and normalizing at 1650° F. The bars were then ground down to size and notched. The impact tests were made using the standard procedure as suggested by the American Society for Testing Materials with the standard specimen and a standard vee notch.

* A thesis submitted to Case School of Applied Science for the degree of Bachelor of Science in Metallurgical Engineering, 1937.

† In cooperation with Mr. A. E. Gibson, President, Wellman Engineering Company. Submitted as a contribution to the Fundamental Research Division, Welding Research Committee.

Table 1—Rockwell Hardness Test Standard "B" Scale

		Averaged Hardness
Man-ten Steel		
Parent Metal	As-received	92
Parent Metal	1250° F.	91
Parent Metal	1650° F.	88.5
Fleetweld No. 5	As-welded	78
Fleetweld No. 5	1250° F.	71
Fleetweld No. 5	1650° F.	71
Diffusion Zone F. W. No. 5	As-welded	86.5
Diffusion Zone F. W. No. 5	1250° F.	85
Diffusion Zone F. W. No. 5	1650° F.	83
Shield Arc No. 85	As-welded	79
Shield Arc No. 85	1250° F.	79
Shield Arc No. 85	1650° F.	70.6
Diffusion Zone S. A. No. 85	As-welded	86
Diffusion Zone S. A. No. 85	1250° F.	84
Diffusion Zone S. A. No. 85	1650° F.	84.5
Cor-ten Steel		
Parent Metal	As-received	80
Parent Metal	1250° F.	76
Parent Metal	1650° F.	76.5
Diffusion Zone F. W. No. 5	As-welded	77
Diffusion Zone F. W. No. 5	1250° F.	75
Diffusion Zone F. W. No. 5	1650° F.	74.5
Diffusion Zone S. A. No. 85	As-welded	81.5
Diffusion Zone S. A. No. 85	1250° F.	76.5
Diffusion Zone S. A. No. 85	1650° F.	72
Yoloy Steel		
Parent Metal	As-received	67
Parent Metal	1250° F.	80
Parent Metal	1650° F.	74
Diffusion Zone	As-welded	82
Diffusion Zone	1250° F.	84
Diffusion Zone	1650° F.	67

Rockwell "B" hardness tests were made on the as-welded, stress-relieved and normalized specimens in the parent metal, weld zone and diffusion zone. Results are shown in Table 1. Impact results are given in Tables 2 and 3 in Figs. 2, 3 and 4.

These results show:

1. In the "as-received" condition both Cor-ten and Man-ten had slightly lower impact values when broken longitudinally to the direction of rolling than when broken transversely to the direction of rolling.

2. After stress relieving at 1250° F., the steels had slightly greater impact values when broken in the longitudinal direction of rolling than when broken in the transverse direction of rolling.

3. Stress relieving increased the impact values about 10 ft.-lb. on Yoloy and Cor-ten, but had little effect on Man-ten.

4. Normalization had little effect on the impact values of Cor-ten and Man-ten, but increased the impact values of Yoloy.

5. The plain coated rod produced better impact values than the Mo coated rod.

6. The weld in all cases produced impact values at least 100% greater than the parent metal.

7. Welds produced from a plain coated rod had a finer grain structure than welds produced from an Mo coated rod.

8. The diffusion zone in all cases showed a good penetration of weld into base metal.

9. Heat treatment had very little effect on the hardness of either the weld or parent metal.

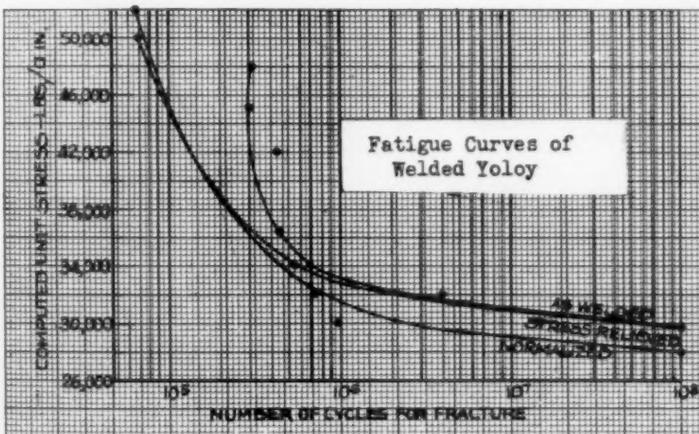


Fig. 1

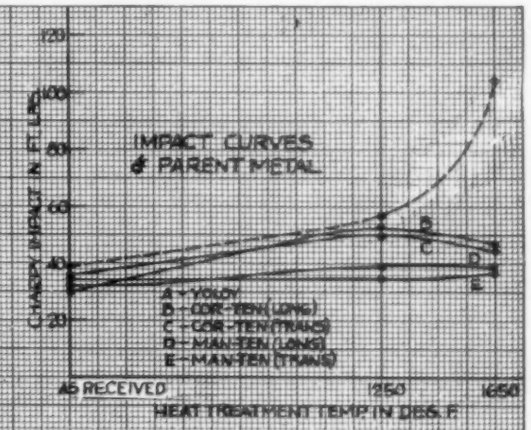


Fig. 2

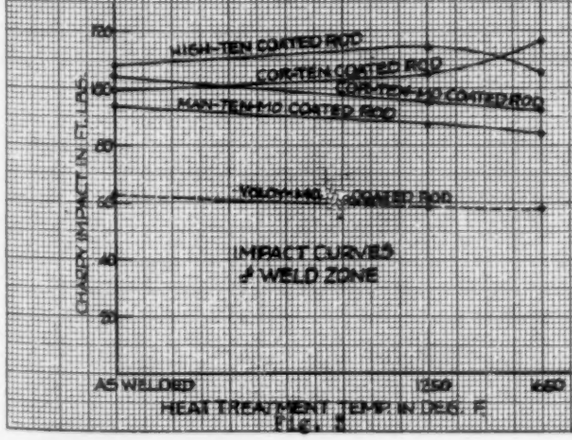


Fig. 3

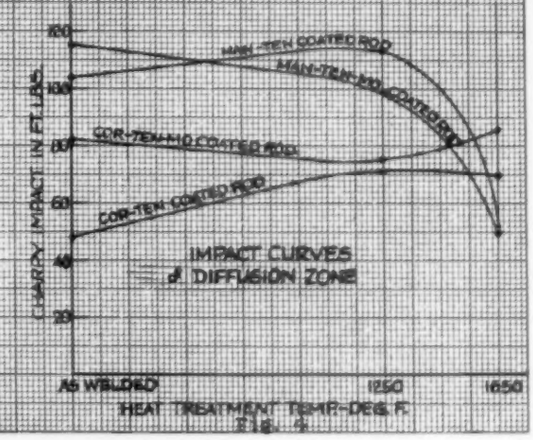


Fig. 4

Table 2—Charpy V Notch Impact Value of Unwelded and Welded Man-ten Steel

Specimen	Electrode	Heat Treatment	Position of Notch	Number of Specimens Tested	Average Charpy Value, Ft.-Lb.	Range of Charpy Values, Ft.-Lb.	Variation of Charpy Value in Per Cent of Average
Transverse	.....	As-rolled	.....	4	35	31–41	29
Longitudinal	.....	As-rolled	.....	4	32.2	28–39	34
Transverse	.....	1200° F.*	.....	4	35	33–37.5	11
Longitudinal	.....	1200° F.*	.....	4	38.5	33–44	29
Transverse	.....	1650° F.†	.....	4	37.2	34.5–44	26
Longitudinal	.....	1650° F.†	.....	4	39.4	37–44	18
Welded	Mo‡	As-welded	weld	3	94.2	86.5–104.5	19
Welded	Mo‡	As-welded	diffusion zone	3	115	95–135	35
Welded	Mo‡	1200° F.*	weld	3	87.8	77–100	26
Welded	Mo‡	1200° F.*	diffusion zone	3	99.6	82.5–116	34
Welded	Mo‡	1650° F.†	weld	2	83.5	82.5–84.5	2
Welded	Mo‡	1650° F.†	diffusion zone	2	50.5	45.5–55.5	20
Welded	Low Carbon**	As-welded	weld	3	108.2	106.5–111.5	5
Welded	Low Carbon**	As-welded	diffusion zone	3	104	90–115	24
Welded	Low Carbon**	1200° F.	weld	3	114	111.5–118	6
Welded	Low Carbon**	1200° F.	diffusion zone	3	113.3	107–118	10
Welded	Low Carbon**	1650° F.	weld	2	110.2	102.5–118	14
Welded	Low Carbon**	1650° F.	diffusion zone	2	49.3	42.5–56	27

* Furnace cooled from 1200° F.
† Furnace cooled from 1650° F.
‡ Covered electrode containing molybdenum.
** Covered low-carbon electrode.

Table 3—Charpy V Notch Impact Value of Unwelded and Welded Cor-ten Steel

Specimen	Electrode	Heat Treatment	Position of Notch	Number of Specimens Tested	Average Charpy Value, Ft.-Lb.	Range of Charpy Values, Ft.-Lb.	Variation of Charpy Value in Per Cent of Average
Transverse	.....	As rolled	.....	4	36.1	33–42.5	26
Longitudinal	.....	As rolled	.....	4	30	30	0
Transverse	.....	1250° F.*	.....	4	50.2	42.5–55	25
Longitudinal	.....	1250° F.*	.....	3	52	44–61.5	34
Transverse	.....	1650° F.†	.....	3	44.5	42.5–45.5	7
Longitudinal	.....	1650° F.†	.....	3	47	45.5–50	10
Welded	Mo**	As-welded	weld	3	104.5	104.5	0
Welded	Mo**	As-welded	diffusion zone	2	82.5	80.5–84.5	5
Welded	Mo**	1200° F.‡	weld	2	94.5	94.5	0
Welded	Mo**	1200° F.‡	diffusion zone	4	75.5	71–79	10
Welded	Mo**	1650° F.†	weld	2	91.2	82.5–100	19
Welded	Mo**	1650° F.†	diffusion zone	2	86	86–86.5	1
Welded	Low Carbon‡‡	As-welded	weld	3	99.5	98.5–100	2
Welded	Low Carbon‡‡	As-welded	diffusion zone	3	48	45.5–51	11
Welded	Low Carbon‡‡	1200° F.‡	weld	2	104.5	104.5	0
Welded	Low Carbon‡‡	1200° F.‡	diffusion zone	2	72	71–73	3
Welded	Low Carbon‡‡	1650° F.†	weld	2	115.5	115.5	0
Welded	Low Carbon‡‡	1650° F.†	diffusion zone	2	70	45.5–94.5	70

* Furnace cooled from 1250° F.
† Furnace cooled from 1650° F.
‡ Furnace cooled from 1200° F.
** Covered electrode containing molybdenum.
‡‡ Covered low-carbon electrode.

ANNUAL MEETING
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